The WELDING JOURNAL

OCTOBER 1953

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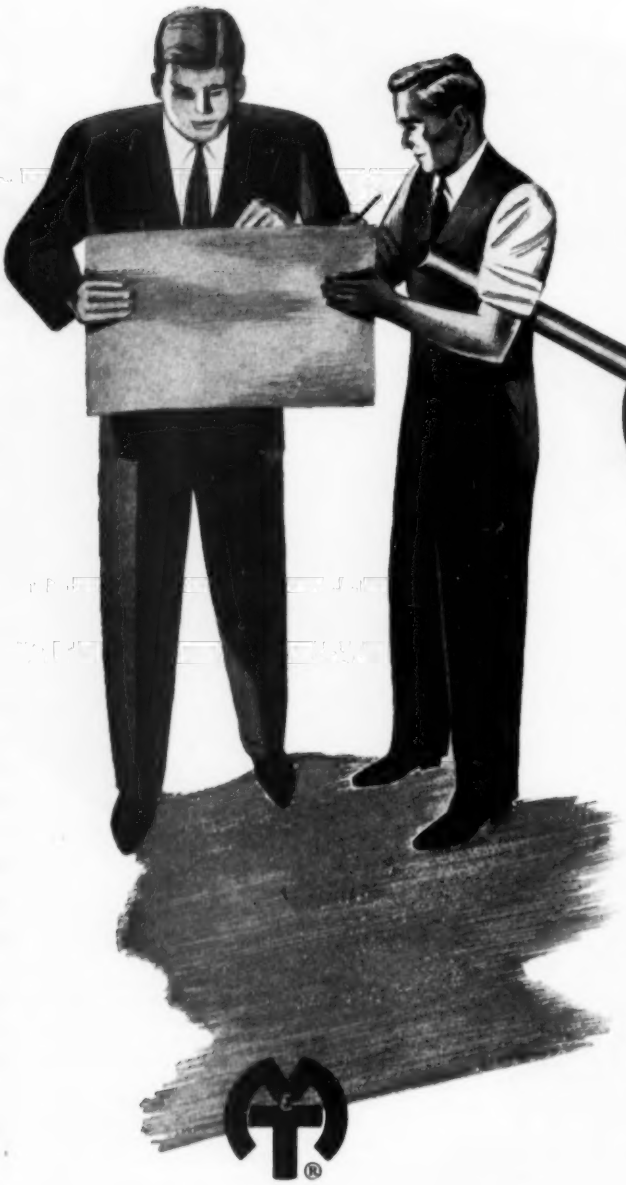

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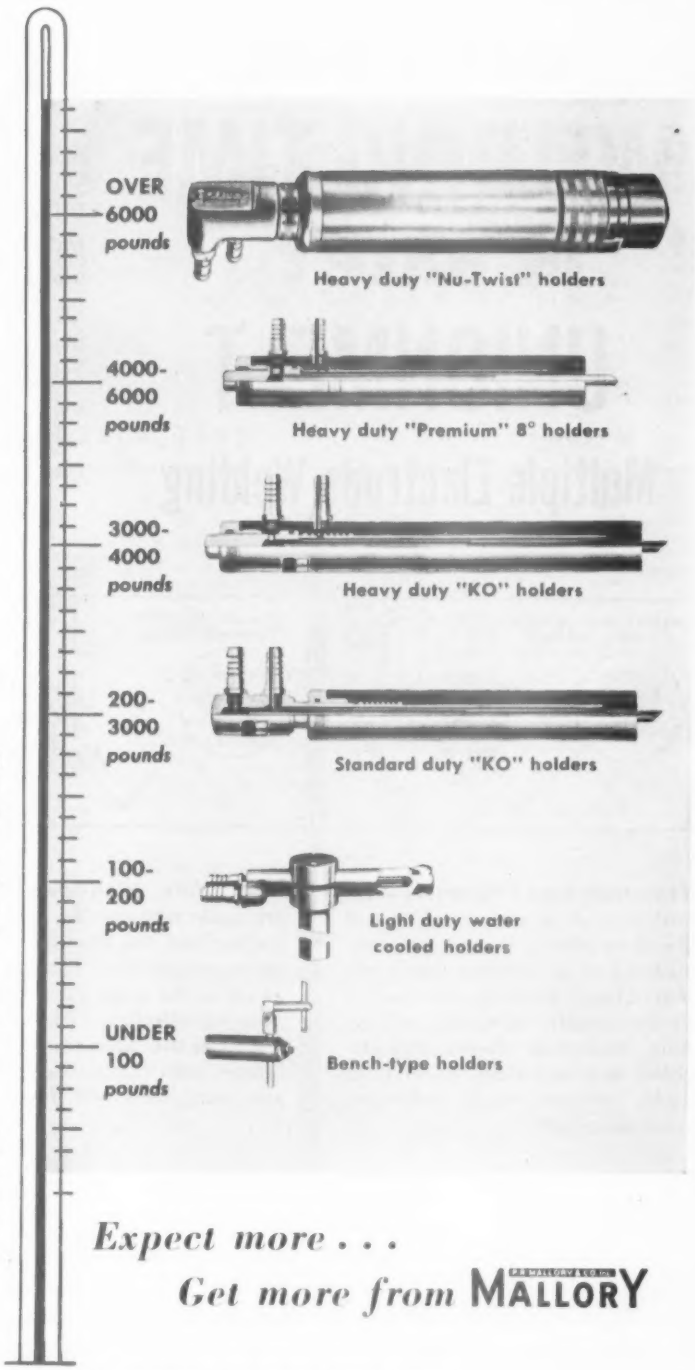

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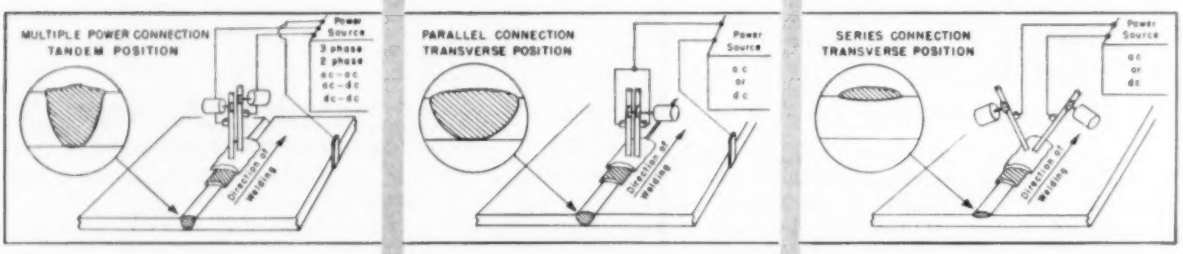

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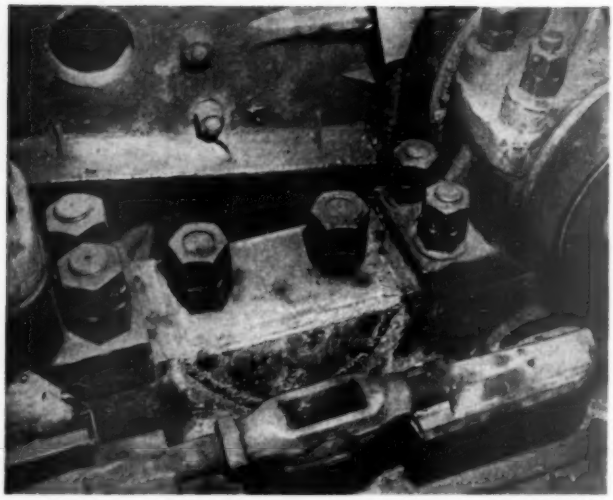

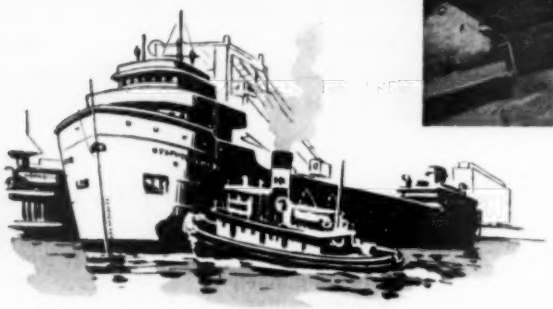

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The WELDING JOURNAL

volume 32 October 1953 number 10

Development of Welded Bridge Construction

The story of the welding of bridges shows a definite and interesting development. This may be traced historically, through research and specifications and through the resultant development of welding practice, to the modern types of welded bridges

by Nathan W. Morgan

HISTORICAL DEVELOPMENT OF THE WELDING OF BRIDGES

ELECTRIC arc welding had its beginning around 1890. Logically, the new process for the joining of metals found its earliest uses in maintenance and repair work. It early became the means for saving broken and worn parts in implements, machines and structures. As the blacksmiths retired, the small welding shops found a rush of business. Early welding experience was introduced to some of the difficulties when it stepped too fast into such fields as automobile springs and high-carbon steel products.

Structural welding appeared in the early part of the 1920 decade. Figure 1 shows the Boston and Main RR bridge at Chicopee Falls, Mass., built in 1927 and usually credited with being the first welded bridge in this country. This 135-ft skew truss span still carries a spur track from the plant of the Westinghouse Electric and Manufacturing Co. From this time onward many welded bridges began to appear in this country, in Europe and in the Australia region. In 1928 a welded railroad girder bridge of 53-ft span was constructed in Pittsburgh, Pa. Each flange consists of three plates varying in both width and length. In 1929 the first arc-welded highway bridge in Europe was built in Poland. This was a pony truss of 88½-ft span. A deck truss span of 122 ft was built in Switzerland in 1930 and a 161-ft through-truss span in Czechoslovakia in 1931. In 1933 a welded girder highway bridge 1040 ft long was built at Dresden, Germany. The girders varied in length from 72 to 86 ft, were 80 in. deep and had single flange plates varying in thickness up to 1 3/16 in. In this year a four-span continuous welded deck truss highway bridge was built in Australia. In 1934, two 150-ft through-truss spans were constructed at Massena, N. Y. A Ramsay Moon in *The Structural Engineer*, November 1934, describes welded construction in Europe at that time, consisting of over 100 railroad bridges and 30 highway bridges.

Fig. 1 Welded railroad bridge, Chicopee Falls, Mass.

Figures 2 and 3 show parts of a 400-ft welded highway bridge built at Burlington, N. J., in 1935. The bridge consists of two 112-ft pony-truss spans and one 160-ft pony-swing span. The swing span carries a concrete counterweight on one lower chord to counter-

Nathan W. Morgan is Principal Highway Bridge Engineer, Bureau of Public Roads, Washington, D. C.

Scheduled for presentation at the AWS National Fall Meeting at Cleveland, Ohio, Oct. 22, 1953.

Fig. 2 Welded highway bridge, Burlington, N. J.

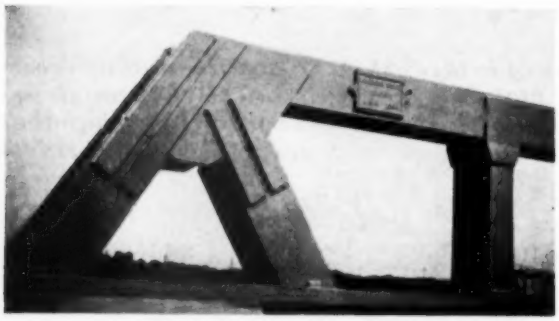

Fig. 3 Welded highway bridge, Burlington, N. J.

balance the sidewalk on the far truss. At this time the Florida East Coast Railway built a 60-ft welded girder bascule bridge at Jupiter, Fla.

The latter half of the 1930 decade and throughout the 1940 decade the welding of bridges progressed and developed scientifically with pronounced acceleration. Welded highway bridges began to be built in New South Wales, Australia, in 1935, and since that time many bridges have been welded. The welded girder bridge opens the door to many possibilities not practicable in riveted work such as structures with curved outlines as used in rigid frames and varying depth girders, and at the same time leads to a conservation of materials and an over-all reduction in cost. A welded girder was recently fabricated in Sydney, Australia, 150 ft long, 18½ ft deep at the center, with 27- x 3⅝-in. top flange and 27- x 3⅜-in. lower flange, ⅞-in. web plate and 24- x 7½-in. T stiffeners. The girder weighs 150 tons. Numerous welded bridges have been constructed by the Province of Quebec, Canada, including continuous girders with spans up to 180 ft. The state of Connecticut built some 25 welded girder and rigid frame bridges. In 1944, two 438-ft railroad-truss spans were constructed in New South Wales over the Hawkesbury River. The members are all-welded, but the field connections are riveted. The longest welded bridge span yet built is the 676-ft plate girder bridge over the Rhine River at Dusseldorf, Germany.

The Bayshore Freeway viaduct in San Francisco which is now under construction is one of the largest welded structures in the world. It involves some 26,000 tons of steel with 2½ million linear feet of welding. The welded girders are supported on steel bents with one or more legs. The single-leg bents may be either T or inverted L in shape. It is reported that considerable saving was affected by welding as compared with either concrete or riveted steel. Most of the spans are short, about 60 ft, but spans to 117 ft are used. Flange plates up to 22 x 2½ in. are used and composite design is used for the main spans. The steel for this project was determined after considerable study and collaboration with the steel manufacturers, the Bureau of Public Roads and others.

DEVELOPMENT THROUGH RESEARCH AND SPECIFICATIONS

The development of structural welding design and construction has arisen out of an intensive program of welding research in all fields, such as metallurgy, filler metal, design, welding technique and welding equipment. Welding specifications have followed the results determined through such research programs.

Arc welding specifications were published as early as 1929 by the AMERICAN WELDING SOCIETY and by Consulting Engineers. These formed an excellent basis for early welding practice. However, bare wire welding was proving to be none too satisfactory and intensive research was in progress in the development of improved types of filler metal. Covered electrodes, which came into use in the 1930 decade, have been one of the most important steps forward in the development of welding science. Concurrently welding machines and equipment have been subject to constant improvement. A parallel development which may be more readily appreciated is seen in our modern automobiles and airplanes, as compared with those of the 1920 and 1930 decades. The completion of the AMERICAN WELDING SOCIETY standard symbols and definitions played an important part in the progress of welding.

In the fields of design, metallurgy and welding technique, structural welding has made rapid strides. Basic research in design, metallurgy, welding processes and technique has been carried on by The Welding Research Council since its inception in 1935. The work of the various committees with their research programs has formed the actual basis upon which structural welding has progressed. No small amount of credit is due many public and private agencies, too numerous to discuss here. Machine welding has been one of the stepping stones toward modern welding efficiency.

Studies in weldability have been of prime importance in the development of welding. It is here that we discover the fundamental principles involved and the causes and methods of overcoming many phases of welding difficulties. Structural welding would be impracticable without this knowledge of the weldability of steels of varying chemical characteristics. The welding of dynamically loaded structures without a thoroughly weldable base metal could result in failures that

would retard progress in welded construction. In fact it is the opinion of some that the development of structural welding in this country has been delayed many years because of the lack of a suitable structural steel for welding.

This discussion would be incomplete without a mention at this place of the slow and painful development of the proper base metal for welded bridges. The proposed specification for base metal for the next edition of the AWS Bridge Specifications represents the current result of several years of effort in this direction and is perhaps the best compromise possible at this time for a weldable A7 type of structural steel with restricted carbon content. When this specification is coupled with careful workmanship, it is believed that satisfactory welding will be the result.

DEVELOPMENT OF WELDED BRIDGE PRACTICE

Welded bridge construction has seen an interesting evolution of practice. In the early part of the 1930 decade the tendency was to follow the beaten path of riveted construction, the thought being that welding was simply replacing riveting. Before any great progress in design was accomplished, it was realized that welding design was a new thing and that structural engineers needed to forget much of the principles of riveting design and seek to develop a new principle and that was to mold steel plates and shapes in as simple and efficient manner as possible along the lines of stress travel and to use as little welding as possible to develop the members and loads to be carried. Curved parts were to be not only more permissible but were desirable, and sharp corners and breaks in stress path were to be eliminated.

Early welded bridges were frequently of the truss type because trusses were in vogue in that period. Also for shorter spans, the construction consisted of rolled beams reinforced with welded cover plates of varying width and thicknesses to facilitate down-hand fillet welding. This gave rise to the practice of using wider plates below, for both flanges, for cases where the welding was done in the field. In the 1940 decade, truss bridges began to give way to the plate girder type as a first choice in design, particularly in the shorter span range, largely to fulfill the principles of esthetics and because of greater ease in the future widening of deck girder spans, so in this period we find the welded girder bridge coming into the foreground in welded bridge practice. While welded girders have not approached riveted girders for length of span, in this country, the welded girder has advanced to a place of competitive importance in highway bridge construction.

The elimination of one other factor in the historical development of the modern welded girder should be mentioned and that was the early partiality for fillet welds and the distrust of butt welds. Bare wire welding lacked ductility and there was reason for questioning the advisability of using butt welds for principal tension splices. After the advent of the covered electrode with its superior ductility in the weld metal and after fatigue tests proved the superior efficiency of butt welds as compared with fillet welds, we find butt welds used freely to carry principal tensile stresses.

The rolled structural beam is the most efficient structural steel section yet devised for the carrying of loads as a beam. The utility of this type of bridge is limited only by the size and weight of beams that can be produced in the steel mills and handled efficiently. The next step for bridge spans beyond this limit was the development of the riveted plate girder bridge. This consists essentially of five parts, two angles for each flange and the web plate. Carrying capacity is increased by the addition of one or more cover plates riveted to each flange. Cover plates over 1 in. thick have seldom been used because of the difficulty in stress distribution and the cost of fabrication. Stiffeners for riveted girders are inefficient because of waste of metal in the legs of angles or other shapes against the web plate for riveting because of the use of filler plates and grinding and crimping for fabrication. Riveted girders are wasteful of metal also because of the punching of rivet holes in tension flanges and the use of fillers and superfluous parts. The curving of flange angles in riveted work is a costly operation.

The modern welded girder exceeds the rolled beam in efficiency, considering the distribution of metal for the carrying of loads, but the cost is greater on account of welding and the necessity for stiffeners for the thinner web of the welded girder. The same efficient distribution of metal is maintained for long-span welded girders. Holes in tension flanges are eliminated. The principal parts are reduced from four angles, cover plates and a web plate to just two flange plates and a web plate. Crimped stiffener angles are replaced with a single bar or plate, or a tee section for heavy work with the flange turned outward, making for greater efficiency than with the flange against the web plate. Each flange consists of a single plate increasing in thickness toward the center of girder, the ends of each plate being butt welded to the next flange plate. Each flange is welded into a continuous unit, likewise the web plates, before the three continuous units are assembled into girder shape for fillet welding. Splice or strap plates are not used to reinforce butt welds because tests made at the University of Illinois indicate that the simple butt splice is stronger in fatigue because of the weakening effect of the fillet welds on the splice plates.

The welded girder eliminates many of the inefficiencies of the riveted girder, but at the same time introduces some new problems. Attention is called to the results in the fatigue testing of rolled beams with partial length cover plates and cover plates with intermittent fillet welds. Both of these types of construction have been shown to be inefficient by the tests made at the University of Illinois for the Welding Research Council. Where dynamic loadings are involved, both of these types should be avoided in so far as practicable. These tests indicate that for full

length cover plates intermittent fillet welding is about equivalent to riveting. However, for partial length cover plates the tests indicate that riveting is better than continuous fillet welding and that continuous welding is better than intermittent. Apparently the reason for the low efficiency of intermittent fillet welding when applied to partial length cover plates is that the intermittent welding results in severe stress raisers. The results of these tests are, in general, applicable to the deeper welded girders where cover plates are used. Welded girders also are confronted with the question of safety of the main strength welds and of preheating and welding technique, which are not involved in riveted construction.

REVIEW OF A RECENT TYPICAL WELDED BRIDGE PROJECT

The past few years have seen the construction in this country of many excellent welded highway bridges. These projects differ in various respects and each involves certain items of interest. A typical project has been chosen for the purpose of illustrating this modern type of welded highway bridge. Discussion will be confined principally to the welded girders.

In 1951 a welded highway girder bridge was constructed over the Wind River at Shoshoni, Wyo. The bridge consists of three 100-ft welded girder spans, each span containing four composite-type girders with the concrete floor slab anchored to the upper flanges by means of angle shear developers. The structure carries a 30-ft concrete roadway with steel handrails and is designed to carry the H20-44 loading on two lanes. The girders rest upon reinforced concrete abutments and piers. The abutments rest upon timber piles and the piers are founded on the underlying sandstone. The project was jointly financed by the State of Wyoming, the Bureau of Public Roads and the Bureau of Reclamation which constructed a dam a short way below the bridge site. The contract specifications were the American Association of State Highway Officials Standard Specifications for Highway Bridges 1944 for design and the Wyoming Highway Department Standard Specifications 1948 for construction. The AMERICAN WELDING SOCIETY bridge specifications were used for welding design and construction.

The welded girders shown in Fig. 4 are 101 ft, 1 in. long or 100 ft center to center of bearings. They consist of a 48- x $^3/_8$-in. web plate, 16- x $1^1/_2$-in. upper flange and the lower flange plate varying as follows from

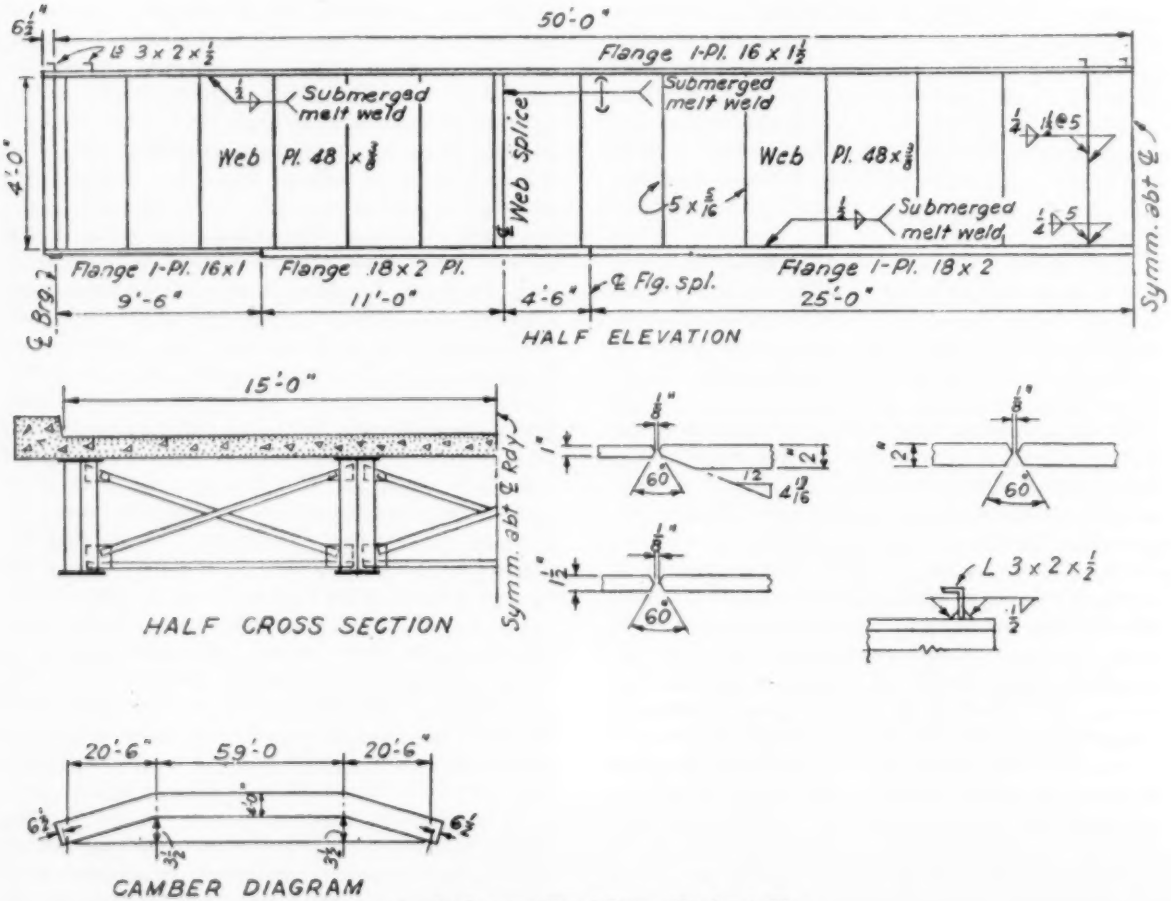

Fig. 4. Bridge over Wind River, Shoshoni, Wyo.

end to center of girder: 16 x 1 in. x 10 ft, 18 x 2 in. x 15 ft, 6 in. and 18 x 2 in. to center line of girder. End stiffeners over shoes are two plates $7\frac{1}{2}$ x $\frac{3}{8}$ in. Intermediate stiffeners are two bars 5 x $\frac{5}{16}$ in. The upper flange carries 3 x 2 x $\frac{1}{2}$ in. x 1-ft, 4-in. angles spaced about 2-ft centers and welded to girder with 3-in. leg vertical and 2-in. leg turned up for anchorage into the 8-in. concrete deck slab. The web splices consists of square butt welds submerged melt process and flange splices consist of 60-deg double-vee butt welds made with extension bars. Fillet welds between web and flanges are continuous $\frac{1}{2}$-in. submerged melt fillet welds. The plans called for simultaneous submerged melt welding on opposite sides of the web but later changed to welding on one side at a time. Fillet welds on end stiffeners are $\frac{1}{4}$ in. all around and on intermediate stiffeners $\frac{1}{4}$ in. intermittent $1\frac{1}{2}$ in. at 5-in. centers and welded to upper flange but not to the lower tension flange. Intermediate cross-frames are spaced 20-ft centers with field connections bolted. The bridge shoes are welded from structural plates. The girders were given a camber of $3\frac{1}{2}$ in. at the time of fabrication by cutting the web plates on a bevel at the splices. The length of the upper edge of the end web plates was $\frac{11}{16}$ in. greater than the lower edge. Fabrications was done by the American Bridge Co. at the Gary, Ind., plant.

The flange plates were of silicon-killed structural steel and the other parts of structural carbon steel. Structural steel conformed to the requirements of the American Welding Society. Material over 1 in. in thickness was checked to ascertain whether or not the carbon content plus one-sixth of the manganese content exceeded 0.40%. The specifications require that when this percentage is exceeded the specified workmanship clauses be applied, involving preheating. A typical mill test report from the South Works of the United States Steel Co. shows the following ladle analysis: for 1-in. plate, 0.25% carbon, 0.47% manganese; for $1\frac{1}{2}$-in. plate, 0.27% carbon, 0.54% manganese and for 2-in. plate, 0.24% carbon, 0.46% manganese and 0.21% silicon. Samples were taken from 24 plates for a check analysis. Each sample consisted of drillings from ten $\frac{1}{2}$-in. diameter holes drilled in close proximity to each other to a depth of $\frac{1}{2}$ in., the first $\frac{1}{8}$ in. of each hole being discarded because of the presence of mill scale and foreign matter. Typical check analyses are as follows: for $1\frac{1}{2}$- and 2-in. flange plates, 0.29% carbon, 0.54% manganese, 0.21% silicon, 0.017% phosphorus and 0.044% sulfur.

A total of 115, 270 lb of structural steel was used on each span or a total for the bridge of 173 tons. The bid price for the completed steel girders in place was $15\frac{1}{2}$ cents per pound, which represented about the average price for riveted or welded bridges at that time for similar circumstances.

The development of the science of the welding of bridges is seen to be of value in pointing the way to future development in this type of construction. There are numerous features in connection with bridge welding, where more information is needed. Much research has been done in the past enabling us to make great progress in the field of welded construction. However, it cannot be too strongly emphasized that such investigations must be continued, perhaps on an even greater scale than in the past, if we are to meet the goals of economy and safety which will insure the greater utilization of that most helpful construction tool—"welding."

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Continuous Welded Structures

A survey of welded structures in Great Britain and other European countries

by W. S. Atkins and E. M. Lewis

INTRODUCTION

IN THE authors' opinion the trends of structural development in various European countries since World War II have been broadly similar. It is natural, therefore, that he should use, in the first instance, British examples to illustrate the main themes of the paper and, in particular, examples from his own practice—other structures in various parts of Europe being described in less detail to indicate the broad similarities.

In adopting this approach one should perhaps first indicate such significant differences as exist between postwar construction in Britain and in the rest of Europe. During the war it has been authoritatively stated that Britain devoted a greater proportion of her industrial potential to war than any other belligerent country. Her plant, converted for war production, was driven relentlessly and there was virtually no new building and no replacement. On the other hand, she was not overrun and therefore suffered no direct devastation except by air attack.

In Britain, at the end of the war, the problem was to reorganize industries for peace production and at the same time make up vast arrears of building and equipment. Basic materials were scarce, labor was dispersed and scarce and, not surprisingly, after six years of intensive effort there were many symptoms of war weariness. However, on the credit side there had been tremendous technical developments during the war and as demobilization took place large numbers returned to peace time work untrammeled by traditional ideas and keen to apply new techniques and new approaches to old problems on the largest scale in industry. The same attitude applied to the planning, organization and progressing of large and ambitious industrial projects. Many of these enterprises were greatly helped by generous aid from the United States and there was a determination to get the maximum benefit from this assistance. To illustrate this resolve and the accompanying sense of urgency, we might here quote one fact relating to the large steel plant which will be described in some detail later. Out of the total cost of 170 million dollars, 27 million dollars were taken up by the civil engineering work. Virtually the whole of the rolling plant was purchased in the United States and within three months of receipt of the last plant information the foundation work was finished and the steel superstructure rapidly followed—indeed some plants came into production before delivery of the mill machinery had been completed.

The broad trends may be summarized as a striving for economy of basic materials, particularly of steel, while at the same time meeting demands for ever greater facilities and amenities—the demand for greater clear spans, for a trouble-free structure, for better natural lighting, for a pleasing appearance and so on. These have led to the increasing use of welding and its associated fabricating processes, the adoption of which in turn enables the designer to push forward the evolution of his structure.

THE ABBEY STEEL MILL—FOR THE STEEL COMPANY OF WALES LTD.

This project involved the construction of a steel mill for the production of slab plates and strips at an annual rate of a million and a quarter tons on a narrow marshy coastal plain bounded along one side by the sand dunes and on the other side by the Welsh foothills. A plan of the scheme is given in Fig. 1. The scale of the buildings involved can be judged by the fact that the main continuous mill building is just three-quarters of a mile long and that together with the Melting Shop and Cold Reduction Mills the buildings roughly fill a rectangular area approximately a mile long by a third of a mile wide—there are no blast furnaces or coking plant, the hot iron being supplied by a reconstruction of the old Margam Works some two miles north.

In planning the structure the engineers were guided

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by experience in new steel mills in the United States and also, to some extent, by experience in older mills in Germany. So far as the material of construction was concerned, all the factors pointed toward the selection of structural steel for the buildings. The form of the buildings was not, however, clear, and it was after much deliberation that the high-low form of construction was selected. The major disadvantage which had been noted hitherto in this form of construction was the uneven lighting, but it was felt that this was not a fundamental difficulty and that it could be overcome as indeed it was.

Welding and Riveting

With regard to the method of fabrication, the engineers had no preconceived ideas. Although in the past virtually all heavy construction of this type had been riveted they were prepared to consider welding as an alternative. From the survey which had been carried out of American and German experience, it appeared to the engineers—as a salient fact—that rolling mill and melting shop structures cost their owners a great deal either in continual maintenance work or, if this is neglected, in the consequential delays due to failures. To enlarge upon this point, it would seem reasonable to regard the building structure up to the level of the gantry cranes as an essential piece of the production plant, only the structure above this level being the usual weatherproof lid which is all most other shed buildings amount to. Viewed in this light and considering the various troubles reported with similar structures of riveted construction—for example, rapid wear of gantry rails at joints, fatigue failures and working of rivets, particularly where gantry girders butt on to the structure, misalignments of the gantry track in service and troubles due to rapid corrosion in bad atmospheres—it became apparent that a stiff, continuous and, what we might term, "monometallic" structure, should provide the best technical solution. Further, such a structure would meet the need for saving steel. These factors taken together led the engineers to select welding as the means of fabrication in preference to riveting. As design work proceeded and the fundamental aim of producing a trouble-free building was related to points of detail design, the conviction that welding was the right choice was strengthened to certainty.

Unit Dimensions

To satisfy plant requirements two unit dimensions were established. This led to the optimum amount of repetition in the building structures. These dimensions were 55 ft for the heaviest building, the Melting Shop, and 40 ft in the mill buildings. These dimensions established the spacing longitudinally for the main transverse frames. Except where particular plant requirements could not be satisfied with these standard dimensions, and these were fairly rare cases, the two standard dimensions mentioned above were adhered to rigidly. Transversely, the frame spans were the minimum possible for floor space to suit the installed plant.

Referring again to Fig. 1, since the plant comprises long, continuous lines, whether in furnace lines or rolling mill lines, the structures are essentially long gantries surmounted by a weatherproof roof structure. The

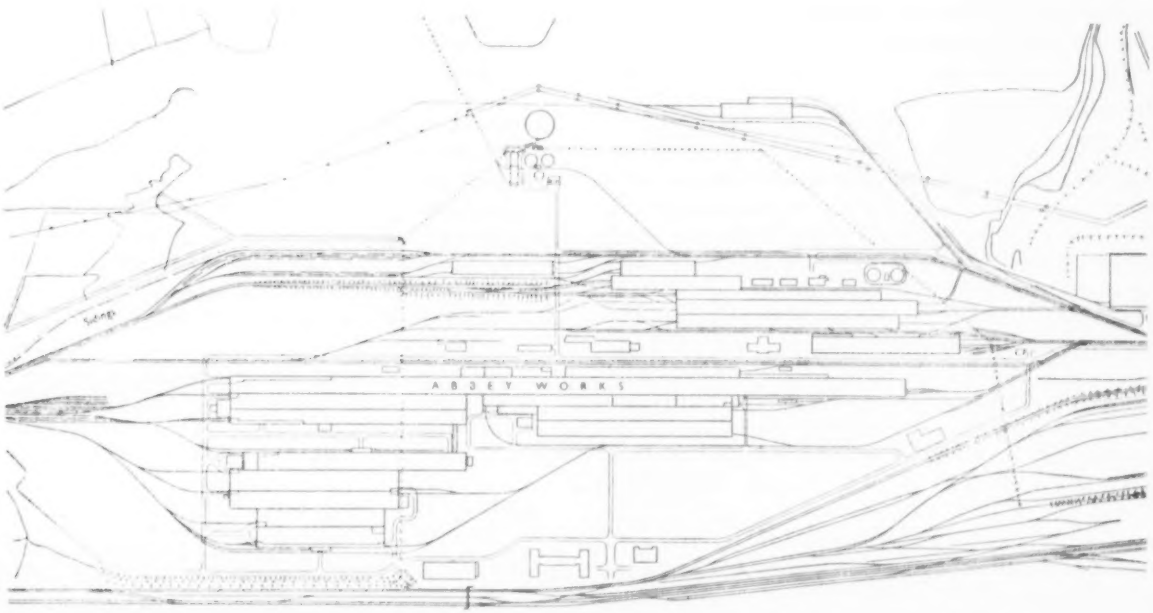


Fig. 1 General arrangement of Port Talbot, Margam and Abbey Works—Great Britain

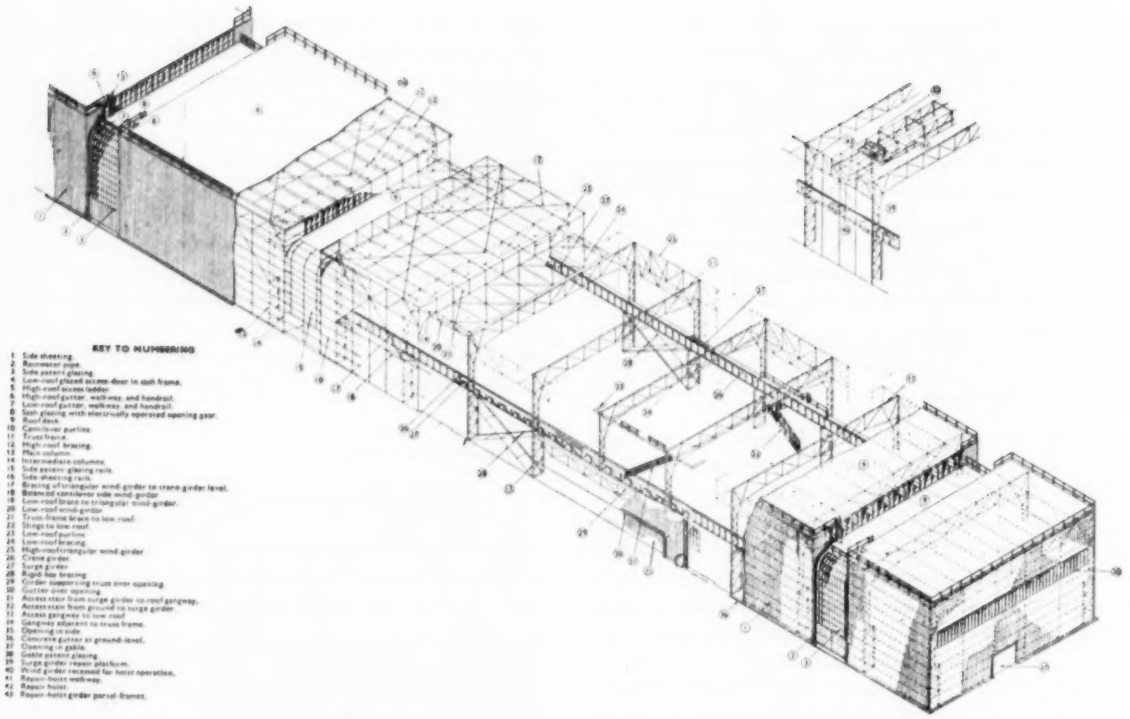


Fig. 2 Isometric view of mold-preparation bay

long structures are in single and multiple spans. The spans and the length of the building are determined wholly by plant and the continuity of the process. For instance, the "Mill Bay" comprises an ingot stripping shop, soaking pits and continuous mill stands down to sheet finishing. This is the longest of all bays and is nearly three-quarters of a mile long. The principles of construction apply to all buildings and the high-low form is adopted everywhere.

Framing Principles

The Mould Preparation bay can be taken as the simplest example to describe the general framing principles which run throughout the whole of the work; the construction in isometric outline is shown in Fig. 2 which, incidentally, also shows the cladding and glazing arrangements. This building is 880 ft long by 93 ft wide by 63 ft high. To obtain the desired degree of strength and stiffness transversely with the minimum weight of steel, the main portal frames are made fully continuous in the transverse direction and are cast monolithically into the concrete substructure. Longitudinally the rather unusual structural system was adopted which is illustrated by the side elevation diagrams at Figs. 3 (*A*) and (*B*). In order to eliminate troublesome joints in the gantry track and to give steel economy, the gantry girder is welded up into long continuous lengths—in this case the girder is continuous for the whole 880-ft length of the building—and the longitudinal forces are transmitted down to the foundations by very rigid braced bays at mid-girder length. However, to avoid high stresses in the transverse frames situated considerable distances away from the braced bay due to thermal expansion, the frames are pinned at gantry girder level in such a manner that though their continuity in the transverse direction is unaffected, they hinge longitudinally. Thus, the columns up to gantry level are free to flex as cantilevers and the action under thermal movements can be seen exaggerated at Fig. 3 (*B*). The gantry girder is also itself supported on pins or rockers so that neither thermal movements nor flexural movements under the action of the cranes affect the main frames.

Above gantry level pairs of frames are braced to form rigid towers which move with the girder without distortion or stress. It will be seen from the illustration that this arrangement fits in ideally with the high-low roof form—purlins span across the tops of braced pairs

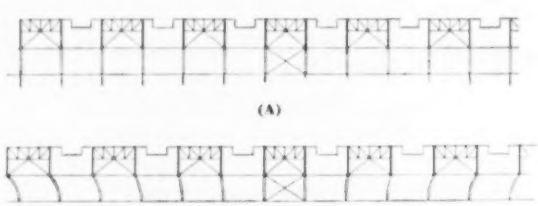


Fig. 3 (A and B) Elevations at mean temperature and under expansion

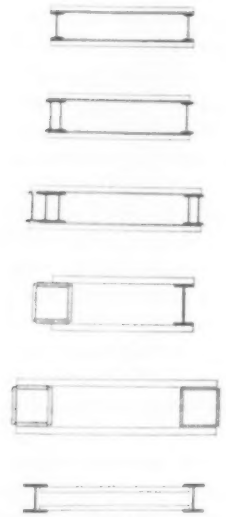


Fig. 4 Typical main stanchion sections

of frames and cantilever over the alternate unbraced bays—the low roof being suspended from the cantilever ends. Any relative thermal movements between the purlins and the gantry girder are taken up without stress by slight swing of the suspenders.

Main Frames

Turning now to consider particular elements in various parts of the building, we may first discuss the main transverse frames which are all of the lattice type throughout, including column legs below gantry level. A Vierendeel type of column leg was considered in the design but was rejected as being more costly to fabricate and being less stiff, weight for weight. The adoption of lattice framing for the horizontal or roof portion of the main frames enabled the framework to be kept unobtrusive and less obstructive to light in addition to being the most economical form. Crane column legs varied in construction from plain joist sections to built-up box plate sections. Figure 4 shows a series of compound stanchion sections below crane girder level.

Gantry Girders

The gantry girders were of triangular box section as shown in Fig. 5. The major vertical loads are resisted in the main by the vertical side of the triangle which is of plate girder form. The remaining two sides are of lattice girder form and provide a torsionally stiff box which greatly enhances the stability of the girder. At the same time it provides a convenient support for the walkway checkered plating, which is clipped on, so that it does not act as a part of the girder and further increase the tilt of the girder neutral axis and hence the tendency to sideways deflection under vertical load.

It is in the Melting Shop that the heaviest steelwork occurs. Here between the casting and furnace bays the crane girders span double units, i.e., 110 ft, to suit the furnaces. Figure 6 shows a section through a pair of girders, one on the west side of the casting bay and one on the east side of the furnace bay. The former girders carry 300-ton ladle cranes and the furnace bay 135-ton capacity chargers. The top flanges of the girders are coupled together as also are the bottom flanges in the way indicated in the figure, so that although under the action of the vertical loads the girders virtually act independently—the box distorting to a diamond shape without inducing high local stresses—they afford mutual assistance in respect of lateral stiffness. The bracing between the bottom flanges also provides support for heavy motor-driven door winches for the furnaces below.

Girder flange plates in the heavy Melting Shop girders which are all of single plate about 36 in. wide, vary from $2^1/_4$ to 4 in. in thickness. Referring again to Fig. 6 it will be seen that a 19- x $2^1/_2$-in. web to flange transition plate, hereinafter referred to for brevity as a "flitch plate," has been used at the top and bottom of the web to ease fabrication problems and also to relieve local stresses under the heavy wheel loads. The depth of this flitch plate varied, being arranged to suit rolling widths for the remaining middle portion of the web, which was kept of constant depth. All the heavy Melting Shop girders adopt the use of web flitch plates, but elsewhere in the Mill and other buildings, where cranes are light and spans smaller (maximum crane capacity 85 tons), webs up to 1 in. in thickness have been used without adopting the flitch plate detail.

Depths of continuous crane girders have been designed for economic weights. This implied haunching to

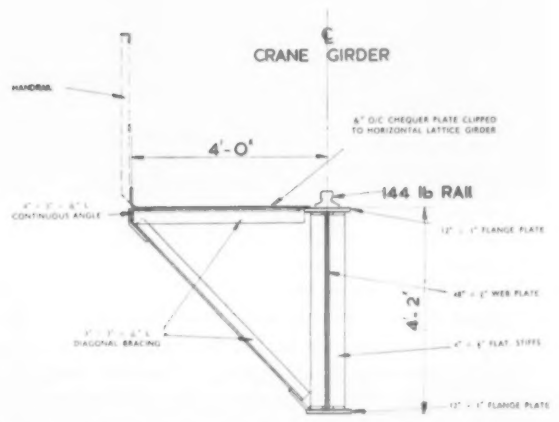


Fig. 5 Typical cross section through mold preparation shop crane girder

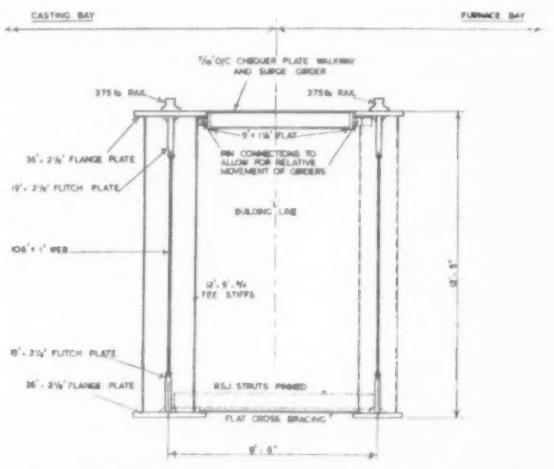


Fig. 6 Typical cross section through melting shop crane girders

greater depths in many cases where one or two long spans occur among a number of shorter ones. Where flange plates vary in thickness or width in one run the heavier plates were gently tapered to the dimensions of the thinner or narrower plates.

Crane Rails

The elimination of the usual girder joint over the columns by the use of continuous gantry girders was considered to be the major step in obviating rail joint wear. It was thought that if the rails themselves were laid on the continuous top flanges and the joints machined square and held close butted, the usual "knock" would be eliminated.

The crane rail profiles were designed in close consultation with The Steel Company of Wales and were rolled by them for the job. They are illustrated in Fig. 7. The bottom flange was designed for fixing to crane girders by fillet welding, although that technique had not been perfected at that stage. Positive fixing by welding was preferred since in the examination of wear on crane rails, which had been pursued before the design of Abbey Works, there was considerable evidence to show

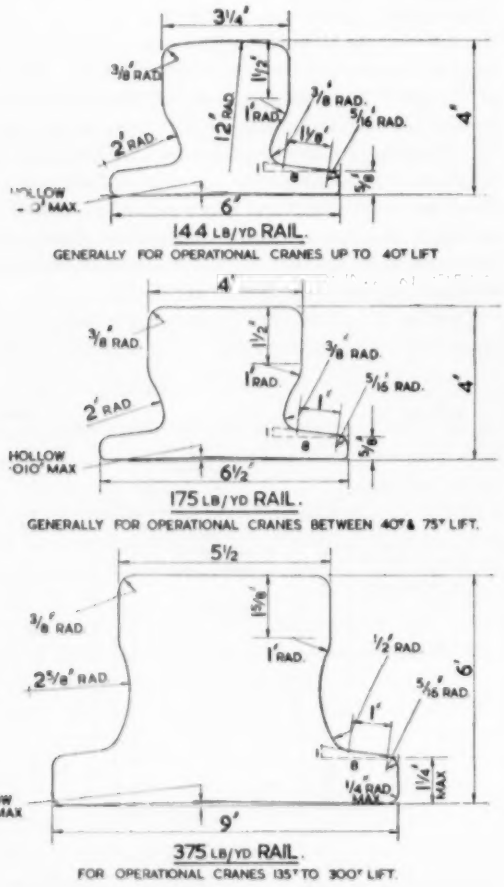


Fig. 7 Special gantry crane rail profiles

that conventional rail fixings tend to "work" in service, the play leading to wear of crane tracks and wheels which gets progressively worse; further welding alone could ensure that rail lengths remained tightly butted in service. It is no more difficult to remove a rail fixed by intermittent welding than one fixed by riveting or bolting.

After much experimental work the following method of intermittently welding medium manganese rail steel to mild steel, both in big masses, was developed. For rails up to 144 lb per yard weight a bead of weld metal was first applied in a downhand position to the edge of the rail flange before assembly, this "buttering run" being made with a ferritic hydrogen-controlled electrode. The buttering run was then welded to the flange plate with a single run of a heavy-gage all-purpose type of electrode, taking care to start and stop this run just within the extremities of the buttering run and to fill up the crater at the end of the run with weld metal. This procedure proved successful for the 144-lb gantry rails, welding being intermittent—6-in. weld alternating with 12-in. gap. The technique was limited to a maximum size of fillet of about $^5/_{16}$ in. and was not suitable for the 175- and 375-lb gantry rails.

For the 175-lb rails with heavier and thicker flanges another method was used, in which the weld metal was deposited in a "mold" formed by a $^5/_8$-in. deep grooved carbon block resting against the edge of the rail flange. A completed weld and the mold are illustrated at Fig. 8. Several passes were made until the mold was filled with weld metal, the operator stopping only to change electrodes, the slag being puddled to the surface and not removed between passes. Again, welding using the carbon block technique was intermittent with 6-in. welds and 12-in. gaps.

Fig. 8 Gantry rail to flange weld

It was found impracticable in the time available to develop a technique for directly welding down the 375 lb per yard gantry rails in the Melting Shop and the following procedure was adopted. The rails were first laid in position between locating pads, which were tack welded to the girders and the ends of each rail secured by means of temporary clips. The overhead cranes

could then travel along the rails and a certain amount of bedding down took place. For permanently securing the rails $^5/_8$-in. thick mild steel pads were first welded to the gantry girder flange on both sides of the rail by means of a $^1/_2$-in. fillet weld. To hold the rails down as tightly as possible prior to welding, one wheel of the overhead crane was brought over the welding position and the crane traveled forward as welding proceeded. For welding the rail to the pads a $^5/_8$-in. multirun fillet was used.

The welds cause the crane rail, which has in many cases a sectional area of the same order as the flange plate, to act as an integral part of the girder. Thus they are subject to shearing loads similar to those between web and flanges. A few welds are, however, subjected to a much more severe loading condition. As has already been mentioned the crane rails are not continuous as are the gantry girders (except in one case), so that a pair of welds next to the rail joint has to transfer nearly the whole of the end-long force in the rail into the flange. To minimize stresses on these welds rail joints were positioned at points of minimum bending, that is where the force in the rail is at the minimum, and these "anchor" welds were also made one and a half times as long as the others. An interesting fatigue problem appeared in connection with these anchor welds, but this will be described in another part of the paper.

VARIOUS TECHNICAL DIFFICULTIES

From the foregoing description it will be appreciated that the design was in many respects novel and certain engineering steps were indeed without precedent. While the theoretical background existed for many of the steps there were inevitably gaps in knowledge between the theoretical approach which could not, by definition, be bridged by practical experience. Indeed it was in bridging these technical gaps that the determination of the engineers to press forward with new ideas was put to the test and the author believes that some of the methods of tackling the problems and the solutions achieved may be of interest to his readers.

Experimental Stress Analysis

The first great difficulty arose in the need to strength analyze in a short space of time, a number of highly redundant main frameworks and of continuous gantry girders over varying spans. At first sight the amount of analysis which had to be done seemed exceedingly formidable. The design of continuous frameworks is essentially a process of trial and error. Thus a design may have to be reanalyzed two or even more times. Even the Hardy Cross method of moment distribution would not meet our requirements. We therefore examined the possibility of an experimental method and decided that development of the indirect model method first devised by Professor Beggs in the U. S. A. would meet our requirements. Already in Britain this technique had been advanced by Professor Pippard who, in 1937, pointed out that it was not necessary to use the very small displacements which had hitherto been the practice—the displacements could be exaggerated as many as a hundred times using plastic models without loss of accuracy if certain precautions were taken. However, until then no extensive commercial application had been made of what was now called the "large displacement indirect model method" in design offices in Europe.

The engineers first tackled the problem of designing apparatus suitable for design office work where a large number of different models had to be analyzed in rapid succession, and in so doing they were able to enlarge the scope and power of the technique in a number of respects. The accuracy of the method as applied to frameworks having very much stiffer lower portions than upper parts was greatly enhanced by the introduction of devices enabling influence lines of bending moment, shear and so on to be obtained for intermediate points in the structure as well as at stanchion feet; certain serious errors inherent in the use of large displacements as applied to continuous girders were tracked down and eliminated by a system of articulated links; finally the scope of the method was extended by enabling the models to be used not only for the production of influence lines by the indirect method but for stiffness determination by directly pulling the model and measuring the forces by devices which compensated for the creep in the plastic. Results of such stiffness observations were not only used as rapid means of designing elements whose stiffness rather than whose strength was the governing factor, but also used in combination with the indirect analysis enabled stresses due to thermal and foundation movements, as distinct from those induced by applied loads, to be readily determined. In practice the use of these models reduced the period of analysis by a factor of three or four while providing ample accuracy for engineering purposes.

It might here be recorded that as construction proceeded a full-scale experimental stress analysis was carried out by loading a pair of frames and observing the strains at numerous points by electrical strain gages. Figure 9 illustrates this work. The experimental observations under full-scale site conditions gave the engineers a high degree of confidence in their methods of design office analysis.

Corrosion Protection

Turning now from the drawing office to a problem which is rather more one of site organization, we come to the anticorrosion protection of the structure. By the use of welding, very much cleaner lines were obtained and many incipient corrosion pockets were eliminated. However, it was felt that this in itself would not provide the freedom from maintenance worries which was aimed at. Technically it was clearly established that the right economic answer to the corrosion problem under the severely corrosive con-

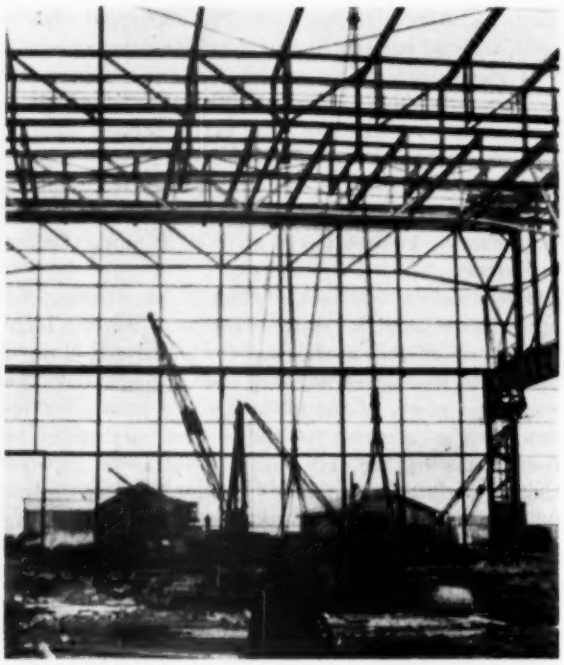


Fig. 9 Frame loading for full scale strain analysis by E.S.G.

ditions which were present on this particular job was to gritblast the steel and then spray with aluminium. The problem of organizing this work simultaneously and in phase with fabrication and erection was one which had, however, not been tackled previously on a scale approaching this. It was felt that the steel above gantry girder level which could only be maintained with great difficulty and expense once the plant was in operation, must be gritblasted and metal sprayed; and this steel fell into two parts of roughly equal weight—the purlins and the frames themselves.

The metal spraying capacity did not at that time exist in Britain to maintain the rate of output necessary to keep up with the fabrication and erection program. It seemed logical, therefore, to seek some method of making the process automatic to increase the capacity. The purlins which were of plain joist section seemed ideal and the first fully automatic metal spraying plant in Europe was designed and erected for this particular job. Figure 10 shows the purlins on the continuous process entering the gritblasting machine and Fig. 11 shows the automatic multigun spraying head with the joist traveling through.

The frames themselves did not lend themselves to automatic treatment but by energetic organization on the part of the steelwork contractor and the metal spraying contractor the units were gritblasted, manually sprayed and delivered to the site on time. Here it should be mentioned that the fully continuous frames were touched up manually at the points of site welding.

Site Fabrication

Now we may turn to another organization problem.

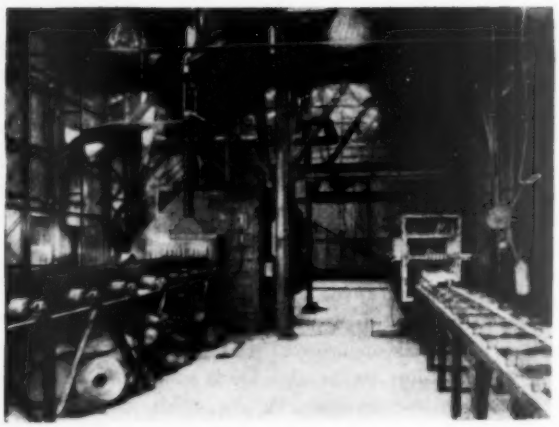


Fig. 10 Set-up for fully automatic metal spray—Great Britain

The main fabricators' works were some three hundred miles from the site, and transport facilities were inadequate to deal with the 110-ft span gantry girder elements for the Melting Shop. It was decided to overcome this difficulty by using the Mould Preparation building—the comparatively small building illustrated in Fig. 2—as a structural steel fabricating shop. A cadre of experienced welders and other tradesmen were introduced from the contractors' main works and the remainder of the labor was recruited locally. Welding courses and training in other branches of steelwork fabrication were given and in the space of a few months the main contractor was able to build up from local resources an efficient structural fabricating shop. Figures 12 and 13 show the large manipulators for the fabrication of these particular elements and a girder being moved from this temporary fabrication shop to its final position in the mill buildings some hundreds of yards away.

Now we may turn to one or two technical bridges which had to be crossed in the welding sphere.

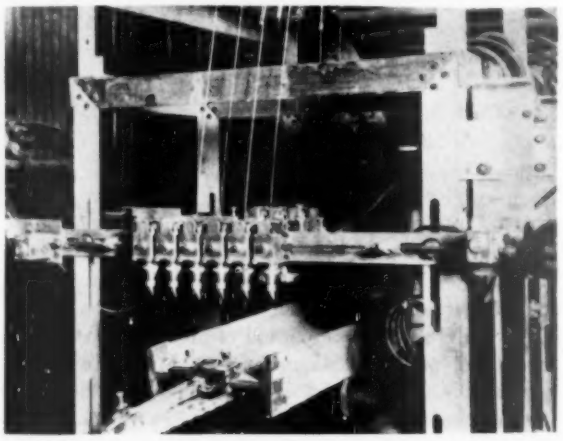


Fig. 11 Close-up of aluminium spray guns for corrosion protection

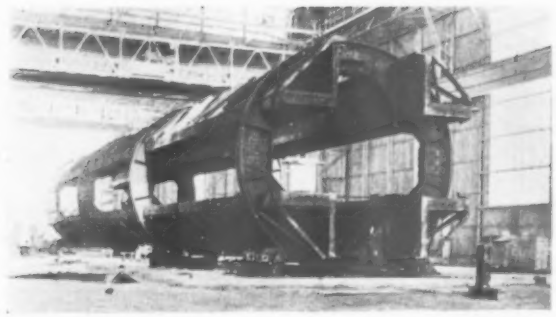


Fig. 12 Manipulator elements for 110 ft. span girder

Fig. 13 110 ft. element prior to erection

Fatigue Test

An interesting fatigue problem arose in conjunction with the anchor welds which have already been described. The reader will recollect that these welds would have to transfer virtually the whole of the end-long load induced in the rail by virtue of its rigid attachment to the top flange into and out of the girder at the rail joint. This fluctuating stress was reckoned to occur several hundred thousand times in the working life of the rail. Further, it was apparent that the build-up of direct compressive and tensile stresses at the end of the weld—at the point of stress concentration—would make matters worse. It will be seen that the stress concentration aspect is closely analogous to the classic rib-to-plate problem investigated in the United States by Professor Hovgaard and in Europe by others, but in this case the problem was further complicated by the fatigue nature of the loading and by the unknown effect of varying weld configurations at the point of stress concentration.

To illuminate this problem the engineers had built up a full-scale fatigue test specimen comprising in effect a stubby I section of two heavy flange plates with the rail block welded in between to act as a web. The welds were subjected to an alternating or shear load by flexing the I beam rapidly, using a resonance method developed for such fatigue tests in Great Britain by the

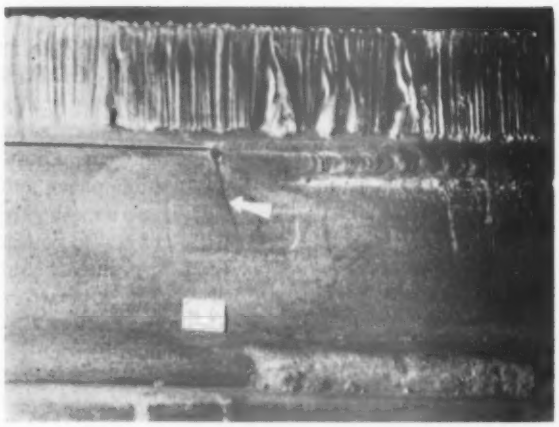


Fig. 14 Fatigue test of gantry rail weld showing crack

British Welding Research Assn. Eventually fatigue cracks developed as expected at the ends of the welds and these can be seen in Fig. 14. But both the number of cycles to failure and the stress range employed in the test showed the detail to have an adequate margin of safety against failure in service.

Ultrasonic Weld Testing

Now we may turn from small welds to some very large ones. It will be remembered that the gantry girders were welded on the site into long continuous lengths of up to nearly 900 ft in the Mould Preparation shop already quoted. For ease of erection these site welded joints were positioned at the columns, the position of maximum stress fluctuation in most cases. Some of the flange plates to be joined were as much as 4 in. thick by 36 in. wide and full-strength butt welds in such plates subject to loadings bordering on the fatigue range were at that time without precedent. After careful deliberation the engineers decided to go ahead with the proviso that they would carry out a 100% non-destructive examination of these welds, which, it must be borne in mind, were being made some 70 or 80 ft up in the air on a site which was notoriously wet and windy. At that time there appeared to be two alternative methods available. Either a million volt X-ray set could be used and a special gantry structure evolved to move it round the site, or gamma rays could be used from a radon source, a landing strip being made on the job so that the radon with its half life of only three days could be flown in from London, then the only source of supply. However, the engineers were enamored of neither method, first because with the very heavy plates they would have little certainty of detecting fine cracks in the first few runs of the multirun welds, and from the economic aspect the inspection was expected to prove inherently costly mainly on account of the delays which might be expected in the erection program due to the lag between completing the weld and the decision as to whether the joint would be satisfactory or otherwise. The engineers therefore sought around for another method.

An instrument which immediately appealed was the ultrasonic pulse echo flaw detector then available in Great Britain based on principles first used by Dr. Firestone in the United States. Here was a device with a beam which would deeply penetrate any thickness of steel without physiological hazards, a light cheap portable device which could work in inaccessible places and follow immediately behind the welders, and a method of inspection which would decide the fate of a weld within an hour or two of completion and in a way which would not form the subject of debate between the contractors and engineers as radiographs so often tend to do. Unfortunately at the time no one either in Britain or America appeared to have done more than a few preliminary laboratory experiments for this application. The engineers therefore decided to establish an ultrasonic laboratory on the site for the specific purpose of evolving an inspection technique suitable for this particular job. Preliminary experiments, occupying only a few weeks, convinced the engineers that they were on the right track but that the apparatus which was then commercially available, and which had never been specifically designed for weld testing, was far from ideal. They therefore added an electronic laboratory and proceeded to design apparatus especially suitable for the particular work. Six months after the establishment of the laboratory a satisfactory technique was evolved which was amply cross checked and calibrated by comparison with radiographs and destructive tests. Thus in time for the site welding, the engineers had available an entirely satisfactory inspection technique which was used throughout the remainder of the job. Though intended originally as an inspection device only, ultrasonic weld examination was found to provide an invaluable control of craftsmanship and very soon the standard of welding rose to a level considerably above that normally obtained in commercial structural welding.

Numerous points in connection with welding technique from the point of view of overcoming distortion, the welding of very heavy plates without preheating and so on had to be met and overcome. We will describe one problem in particular as illustrating this group of "knowledge gaps" and the way in which they were bridged.

Hydrogen Cracking

The carbon block technique for welding down the crane rails has already been described in some detail. Preliminary laboratory work—hardness tests, microexamination and so on—had revealed no fundamental snags. However, after several thousand welds had been satisfactorily completed on the job—many of them being on girders the cranes of which had already been pressed into service to help erection—there came a sudden spate of transverse cracking in the welds. As far as could be seen nothing in the welding technique had been altered and the cause of many failures, some of which threatened to spread into the flange plates, was obscure.

From the outset, hydrogen embrittlement was suspected as a likely cause but where did the hydrogen come from? It might have come from water picked up by the carbon block, it might have come from water trapped beneath the rail prior to welding, it might have come from water in the electrode coating, it might have been present in the electrode core wire or in the rail or in the flange plate. Welding continued while an intensive investigation was carried out to eliminate by deduction from the facts known about the particular welds and by a series of full-scale trials. Eventually it was found that the major cause was hydrogen entrapped in certain "green" rails which had been taken straight from the mill and welded on to the building without an intermediate aging period. Programs were immediately rearranged to look after this and certain other precautions were taken and thereafter the welding technique was continued with success.

Numerous other examples of gaps in technical knowledge which could not be bridged by experience because there was none, could be cited, but sufficient examples have been given to illustrate the methods employed and the scale of the research and development work needed to back up the design office and site organization.

The reader will have inferred from the foregoing that no extensive application was made in the design of this welded steel structure of the intensive research being carried out both in America and in Great Britain into the plastic properties of steel in relation to continuous welded structures. The reasons for this may be noted. It has already been mentioned that from gantry girder downward the structure was regarded as being subject to conditions of usage closer to plant conditions than structural conditions and that the design was viewed from this point of view—that is to say, stress reversal and fatigue considerations dominated. Above the gantry girder level the light, open truss work was governed mainly by questions of elastic instability and these factors together rendered the application of what is termed in Britain the "elementary plastic theory," impracticable. Therefore, as the reader will already have inferred, the designers were guided in their strength analysis by traditional concepts based on elastic considerations.

A PILOT PLASTIC DESIGN

Shortly after this building was completed the engineers were fortunate enough to have an ideal opportunity of putting into effect without compromise under almost ideal conditions a number of theories pertaining to welded building structures, including the theory of plasticity as developed in Great Britain by Professor Baker and his team at Cambridge University. The building concerned was a small fatigue-testing laboratory for the British Welding Research Assn. It will be seen from Fig. 15 that this comprises a main testing hall with a small two-story office block at the north end. It

Fig. 15 British welding research association laboratory Abington, England—first steel framed plastic design erected in Europe

is in the steel structure supporting the main hall that the pilot experiment was carried out.

The main themes of this pilot design, intended to illuminate the economic aspects of certain theories in relation to welded building structures, may be stated as follows:

(*a*) By suitable proportioning, monometallic structures can be designed having sufficient inherent stability (aided by stabilizing influence of the cladding in some instances) to enable the plastic properties of the steel and also the degree of redundancy of the structure to be taken advantage of in the design. In short, the plastic theory might be expected to yield economic benefit.

(*b*) The considerable economies in weight of steel which might be expected from (*a*) above need not necessarily be frittered away in increased labor costs—particularly in relation to the final step, the welding of all site connections.

(*c*) If welding and its associated fabricating processes are used logically, it may be expected that certain traditional forms of element will prove no longer the right economic solution and that therefore new forms may be expected to emerge.

Though perhaps of interest to certain structural designers, it would seem out of place to give many details of this experiment in this paper. The author will therefore merely select certain points from the experiment to illustrate the main themes.

Plastic Design

The diagrams in Figs. 16 and 17 illustrate the main framing scheme. It will be noted immediately that the purlins, the special eaves gutter unit and the gantry girder are welded into a continuous length throughout the building and that the transverse frames are also welded into a continuous structure cast monolithically into the concrete substructure in the same way as at Abbey Works. The transverse wind loads are transmitted from the brick walls to the transverse steel frames via reinforced concrete wind beams in this case, since these conveniently formed cills and lintols and married into the brick cladding more easily than continuous steel members would have done.

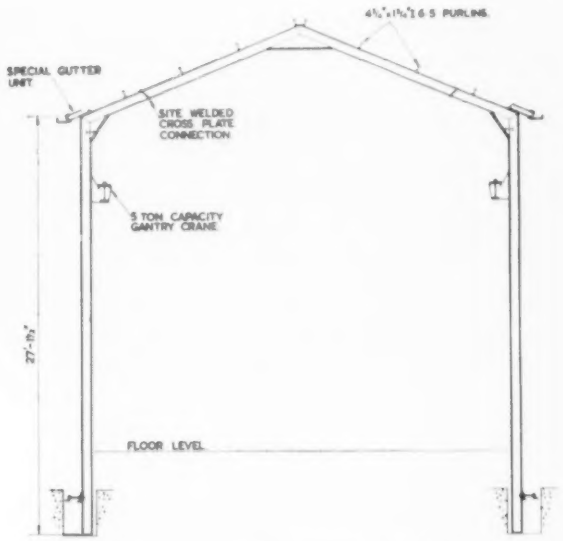


Fig. 16 B.W.R.A. Laboratory main frame

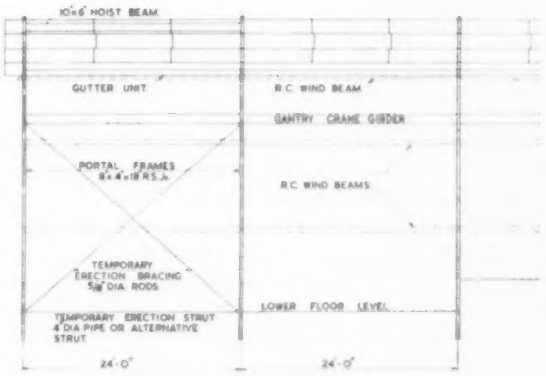


Fig. 17 B.W.R.A. laboratory—framing scheme

The main frames were stabilized up to eaves level by the concrete beams and also by the inner leaf of the cavity brick wall which was built in hard against the web of the main joist section as shown in Fig. 18. At the eaves knee—a critical point in relation to elasto-plastic instability—the joist cutting which can be seen in Fig. 19 was cast integrally into the top concrete wind beam. The rafter member was not only restrained laterally by the purlins but since these were welded to it they also provided a considerable degree of torsional restraint. This framework, if loaded to collapse, could be expected to fail by the formation of four plastic hinges which were found from the calculations to occur at the stanchion feet and at the eaves—in this condition the structure would fail as a mechanism. It would be out of place to describe in detail here the most interesting theory upon which these calculations are based. It should merely be stated that research at Cambridge University under the direction of Professor Baker has been carried forward to the stage where the design office calculations are simpler than their commonplace elastic

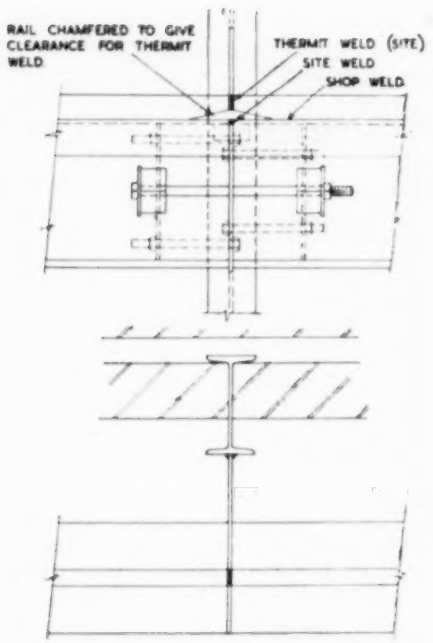


Fig. 18 B.W.R.A. laboratory—site welding details of continuous gantry girder

theory counterpart. A good account of this work is given in the *Journal of the Institution of Structural Engineers* (Great Britain) for October 1949.

Subsequent analysis of the design showed the framework—which complied to the requirements of the British Standard Specification 449 which governs steel building structures in Great Britain—to be approximately 45% lighter than what we would term its simply supported counterpart, i.e., cantilever columns supporting a welded or riveted truss, and at least 17% lighter than the lightest conceivable section which would have been used had the continuous frame been analyzed in line with current elastic design methods. A more realistic estimate would have been around 40% saving but this would have been to some extent contributed to by the restricted range of rolled sections available.

The use of an I section purlin welded into continuous lengths throughout the building and also welded integrally to the frame enabled a considerable weight saving to be made on this item. In fact the purlins used were a little over half the weight of those of the traditional angle sections commonly employed. To some extent this saving is attributable to the fact that the total deflection under working loads was approximately halved by presetting the purlins before welding so that under the dead load, i.e., the weight of the roof sheeting plus lining plus electrical fittings, the purlins became approximately straight. These precambered purlins can be clearly seen in Fig. 20.

Site Welding

The objections often raised to site welding, particularly in Britain where the climate is notoriously uncertain and work on the site may often be held up due to wet weather at any season of the year, are that delays in erection are a serious economic drain. The designers felt that this need not necessarily be so provided that the problem of erection was tackled by them with the same energy as might be applied to the selection of the sections for the main element and to the strength analysis of the structure. It was considered that simple self-jigging single-bolted connections could be devised which would enable the erection of the steel to proceed, if anything, faster than possible with normal bolted connections and that then the framework might remain unguyed for any period awaiting attention from the welders. Figure 21 shows a typical joint of this kind.

Fig. 19 B.W.R.A laboratory—plastic frame knee showing stabilizing lug

Fig. 20 B.W.R.A. laboratory—continuous cambered purlins

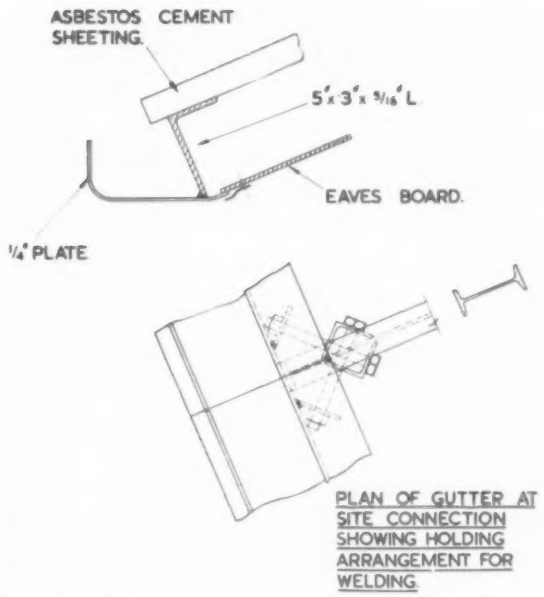

ASBESTOS CEMENT SHEETING.
5" x 3" x 9/16" L.
EAVES BOARD.
1/4" PLATE
PLAN OF GUTTER AT SITE CONNECTION SHOWING HOLDING ARRANGEMENT FOR WELDING.

Fig. 21 B.W.R.A. laboratory-site welding detail, special gutter element

Here the special gutter unit comprising a piece of plate bent and automatically welded to an angle, thus providing not only a gutter unit but a combined eaves tie as well, was attached by one bolt at each end to the small cantilever extensions from the frames, the bolts being so inclined that they had a force component butting the two adjacent gutter ends together and at the same time holding them against the pack, thus jigging them for welding and providing a joint of ample strength requiring no guying.

Scaffolding for site welding the joints in the main frames and in the continuous purlins was provided for no additional cost by utilizing a small platform later to be attached on top of the gantry crane for servicing roof lights, etc., so that it could be slid along the gantry rail to provide a working platform for the welders (see Fig. 22).

Nontraditional Elements

Now we come to illustrate the third theme, that being the need sometimes to break away from traditional forms if welding and associated modern fabricating processes are to be used to maximum economic advantage. The gutter unit to which your attention has already been drawn in another connection provides an example of this. Traditionally, a rolled section eaves tie would be used carrying, by means of clips or brackets, a preformed gutter unit. The section you will already have noticed in Fig. 21 serves both these purposes and provides a stronger, neater and larger capacity gutter than would have been obtained with the traditional approach.

The gantry girder, a cross section of which is shown in Fig. 23, at first appears to be of traditional form. However, it will be noted that instead of the usual joist section surmounted by a channel surmounted by a

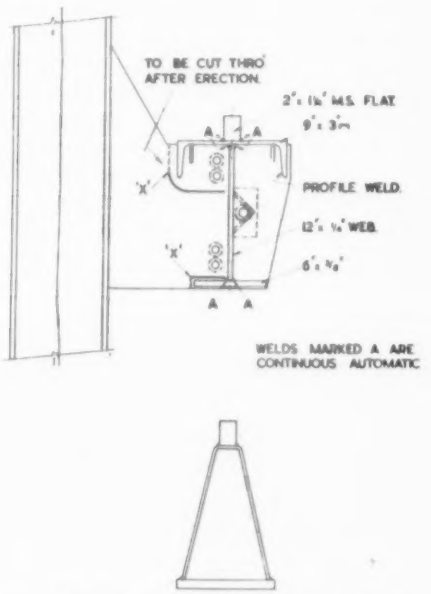


Fig. 23 B.W.R.A. laboratory—gantry girder details

Fig. 22 B.W.R.A. laboratory—site welding

rolled section rail, it comprises a bottom flange plate automatically welded to a thin web plate automatically welded to a channel which in turn is automatically welded to a flat or billet forming an integral part of the top flange and also acting as a rail for the manually operated 5-ton capacity crane. This section, though of traditional form, was approximately 50% lighter than usual, and involved no extra workmanship whatsoever. This somewhat startling weight saving without change of form stems from the following sources. The 1/4-in. web plate is considerably thinner than the web of the

corresponding rolled section would be, the heavy top flange of the rolled section which would be at a position of low stress and therefore contribute a little to the strength of the structure was eliminated, and finally the rail which by welding inevitably acts as an integral part of the structure is made to carry its full share of the load. Finally, the girder is welded into continuous lengths, thus again increasing its strength for a given weight, or conversely, being lighter for a given strength.

Before leaving the evolution of new elements, mention should be made of the torsion bar as a structural element. During the riveting era structures almost invariably comprised simply ties, struts and beams. Considering that when two beams meet at a right angle bending in one induces torsion in the other it may seem surprising that the torsion bar has played little or no part in building structures. The reason is, however, not far to seek. It is very difficult to provide a riveted connection which will readily transmit torsion from one member into another at right angles, and further the open rolled sections evolved during the riveting era are torsionally feeble. With welding, however, the simplest possible method of connecting two members at right angles is probably the profile weld and this will transmit torsion most efficiently. Further, by welding together standard rolled sections either angles or channels, closed box sections of considerable torsional strength and rigidity can be most economically made. One might therefore expect to find in a welded structure a number of torsion bars which are merely the logical complement to the beam.

This small pilot design included four such applications, two of which may conveniently be illustrated here. At one end of the building a small platform was required to support two tanks, one of which might be empty, one of which might be full. Figure 24 shows how such a platform might have been supported in a riveted structure. Two suspenders would have been necessary and stiffening of the purlins or additional members parallel to the purlins would have been required, the tanks being heavy compared with the roof load. The platform, it should be explained, was at one end supported by lugs built into the gable wall. A much more economical and elegant solution would be to suspend the platform from the heavy hoist beam immediately above, if means could be found for preventing twisting when one tank was full and one empty. By making the platform of two channels welded to a

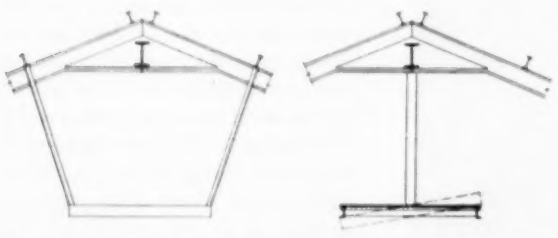


Fig. 24 B.W.R.A. laboratory—torsion bar application

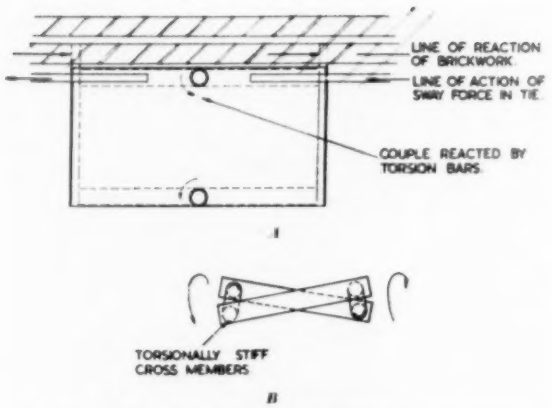

LINE OF REACTION OF BRICKWORK
LINE OF ACTION OF SWAY FORCE IN TIE
COUPLE REACTED BY TORSION BARS
A
TORSIONALLY STIFF CROSS MEMBERS
B

Fig. 25 B.W.R.A. laboratory—torsion bar application

checkerplate platform and connecting the channels transversely by lengths of tube, such a torsionally stiff platform was readily achieved. The action of the tubes in resisting warping is illustrated in the diagram, Fig. 25 (*A*).

At a late stage in the design the designers became aware of a requirement to hang a 9-ton bottle from the gable frame for the purpose of plant erection. A tie was added to the frame at the level of the platform to prevent spreading at the eaves, but resisting the sway force on the frame which might amount to 2 tons, presented a problem. The brickwork at the return to the gable wall, being much stiffer than the frame, would in the first instance take the whole of this load and in consequence probably crack. It was therefore decided to peg the frame to the mid-point of the gable wall via the platform lugs. This could easily be done except that the line of action of the sway force in the tie was eccentric to the line of reaction in the brickwork. Thus the platform would twist on plan, as illustrated in Fig. 25(*B*). By substituting a tube for the angle section suspender previously proposed, the torque was transmitted to the laterally stiff hoist beam above thus providing an elegant and inexpensive solution. It will be appreciated that the final solution involved considerably less material and labor than the traditional riveted solution which would have been the twin suspenders with stiffened purlins with additional cross bracing (not shown on the diagram) to meet the later sway requirement.

Economics of Pilot Design

The examples which have been given are merely illustrative of the main theme but the same trends of thought were repeated over and over again in the design. A cost analysis of the final structure as erected when extrapolated by the contractor in terms of a building at least ten times as big revealed a clear-cut saving of at least 14% as compared with normal present-day British welded frame design. The authors however, believe that this figure is pessimistic and that a saving of about 20% would be more realistic.

While in the design of building structures there are too many variables for far-reaching conclusions to be drawn from such a small-scale pilot design, it greatly strengthened the authors' conviction that the evolution of welded structures both from the technological and economic point of view is far from reaching finality.

OTHER EUROPEAN EXAMPLES

So far the examples given have been taken from the authors' personal experience to show general trends which may now be further illustrated by a number of descriptions of other works in Britain and elsewhere on the European Continent.

French Steelworks

Two steelworks recently completed in France are of particular interest in that their form of construction very closely follows that adopted previously for the Abbey Works. This is particularly interesting in that the engineers concerned must presumably have considered the same problems as the author in planning the work and have independently come to the same conclusions.

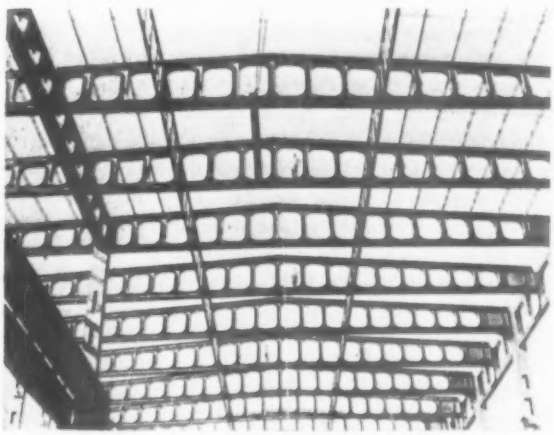


Fig. 26 Vierendeel steelworks construction—Italy

Italian Steelworks

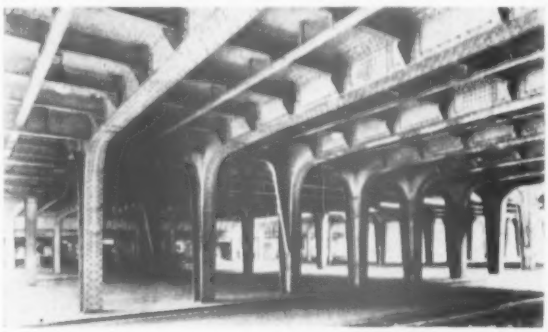
While the extensive use of the Vierendeel form of element in steelworks, even if the booms are tapered, is in the author's opinion likely to prove costly both in labor and material, designers in certain European countries lean toward this structural form which fabrication by welding renders practicable. The illustration of a steel mill in Italy, Fig. 26, designed by A. Bozzarelli of the Ilva Corp. provides a good example of this trend.

German Works

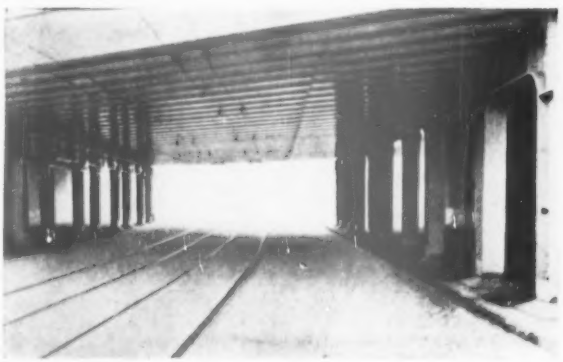
The suspension bridge over the Rhine between Duisberg and Dusseldorf shown in Fig. 27 is of interest because it provides an example of the increasing use by structural fabricators of automatic and semiautomatic methods of welding. The stiffening deck of this suspension bridge is mainly of welded plate girder construction, these elements all being fabricated by the Elin-Hafergut process which we in Britain call "firecracker" welding.

Fig. 27 Suspension bridge with semiautomatic welded stiffening girder—Germany

In Germany, as elsewhere in Europe, designers have been quick to take advantage of the clean simple surfaces to which fabrication by welding leads. The elimination of innumerable small excrescences inevitable in riveted work brings substantial economic gain in the fight against corrosion costs and a less easily evaluated



A



B

Fig. 28 Contrast between riveted and welded railway viaducts—Germany

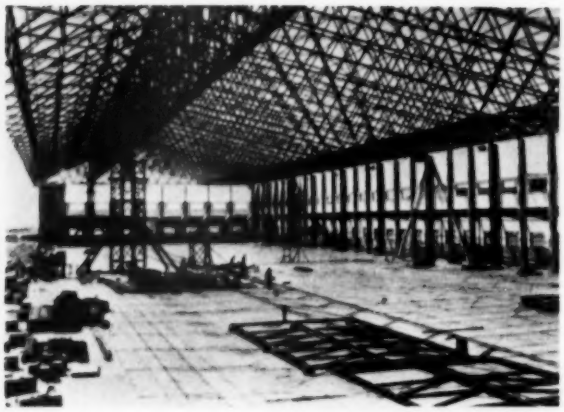


Fig. 29 Site fabricated hangar—Spain

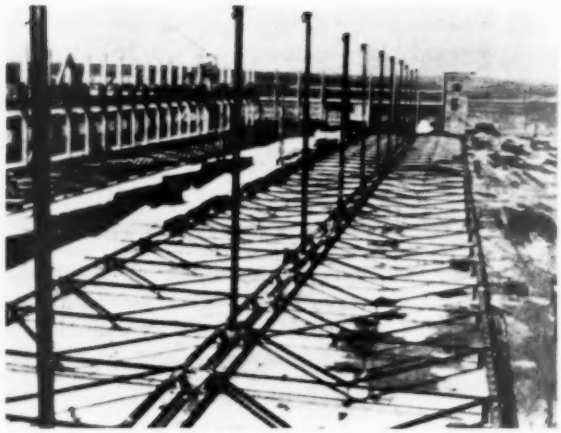


Fig. 30 Site fabricated hangar—Spain

but nonetheless real gain from the esthetic viewpoint. Figures 28 (*A*) and 28 (*B*), showing, respectively, riveted and welded viaducts in Germany, provide a good illustration.

Spanish Hangar

One of the influences of welding which will be enlarged upon later is the trend toward making not only site connections by welding but to carrying out an increasing proportion of fabrication on the site. The hangar illustrated in Fig. 29 provides an excellent example. This structure (near Madrid) designed by Professor Torroja, is approximately 600 ft long by 150 ft deep. The roof is supported by a continuous welded lattice girder with but one internal support in the 600-ft length —this single internal column met a definite functional requirement. This very large lattice girder, weighing approximately 450 tons, was assembled completely by welding on the ground immediately beneath its final resting place. Figure 30 shows two half sections awaiting hoisting after site fabrication.

BRITISH TUBULAR AND STRESSED SKIN WORK

The foregoing examples have been chosen from various European countries for the purpose of showing how the broad trends are the same. Now we may return to Great Britain for examples to illustrate another trend. The role of the structural engineer in Europe which has been largely confined to bridges and building structures in the past is, under the integrating influence of welding, again expanding so that it overlaps other fields of engineering —in the following examples it leads the structural engineer to deal with plant.

That the circular tube or box section torsion bar should take its logical place alongside struts, ties and beams as a fundamental structural element with the advent of welding has already been remarked upon. However, the tube is also a very efficient strut section and is no less efficient than other sections as a tie—only as a beam is it inferior weight for weight. However, apart from considerations of structural efficiency, i.e., weight/strength ratio, the closed box when sealed provides the best possible solution to the corrosion problem. Further, when used in structures subject to large wind pressures its aerodynamic drag is far less than that of the comparable open rolled section. Finally, when used under roof conditions the circular tube particularly is better able to withstand accidental knocks than open-rolled sections of similar weight. It is therefore to be expected that welded tubular structures will prove the best economic engineering choice in certain cases.

The jib of the walking dragline illustrated, Fig. 31, provides an excellent example of the work of the structural engineer applied in the plant field and a perfect example of the use of welded tubes in a structure. It is perhaps not out of place to comment here that this British machine designed by Ransome & Rapier with the jib designed and fabricated by Tubewrights Ltd.—

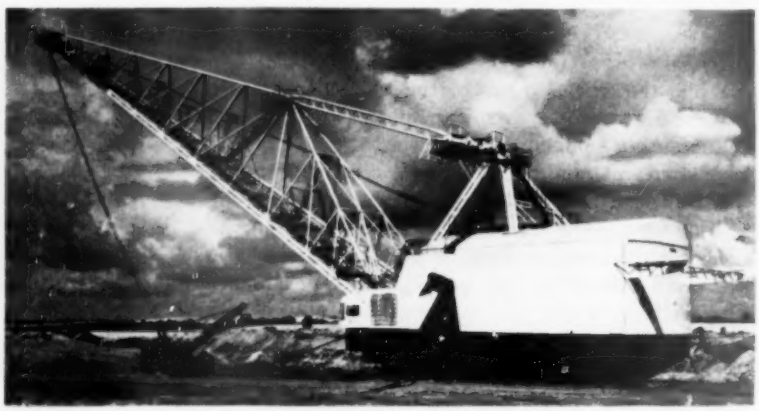


Fig. 31 A 282-ft all-welded tubular steel job of a walking dragline made of high-tensile molybdenum steel—Great Britain

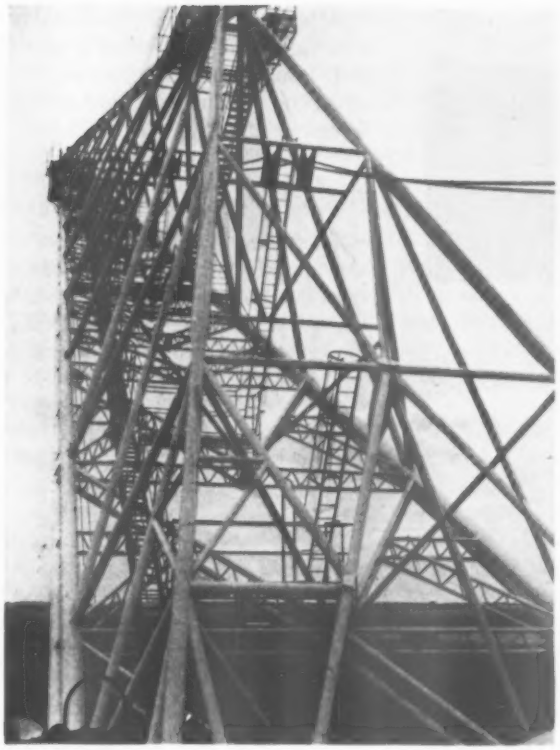


Fig. 32 The "catwalk," comprising inspection ladders and platforms, gives access to all parts of the jib

an organization specializing in tubular building and other structures—is claimed to be the largest in existence. It weighs 1600 tons and has a dumping radius of 260 ft. Its bucket takes 27 tons at a bite and the machine digs and dumps its own weight every hour of its working life.

Fig. 33 (A) View of the Apex Rings and assembly of structure at this point

Fig. 33 (B) Tubular stubs welded to a 1¼-ton steel forging, forming one of the heel pieces

The immense boom, weighing 126 tons, complete with catwalks and accessories, and illustrated in Fig. 32, is of special high-tensile chromium molybdenum tubes and was site welded from shop fabricated 40-ft lengths. A particular feature of the construction which illustrates a trend in structural design which will be further commented upon later is the use of special forgings for certain intricate details. Examples of such a detail and such a forging are shown in Figs. 33 (*A*) and (*B*).

Fig. 34 Skip hoist at Stewarts and Lloyds Ltd., Bilston Iron and Steel Works

This structure was site fabricated, and is of all-welded tubular steel construction. Skip bridge being threaded between stoves on left and the furance on the right. When complete, projection plates will be welded in spaces between rails.

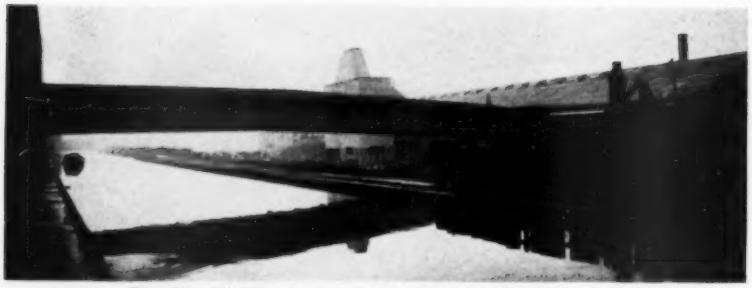


Fig. 35 Welded stressed skin bridge—Great Britain

Another interesting example of tubular work by Tubewrights Ltd. is the blast furnace skip hoist shown in Fig. 34.

To conclude this series of illustrative examples the author would include a small bridge from his own practice which illustrates the corollary to the aforementioned trend, i.e., the influence of engineering thought in other spheres of structural design. From Fig. 35 it will be seen that the bridge comprises a simply supported single span. Its function is to support and totally enclose a conveyor belt between two parts of a factory divided by a canal. A particularly corrosive atmosphere, together with the need for smooth, external faces free of dirt-catching excrescences and details, led naturally to a welded construction. The need for a smooth, easily cleaned interior led to a shell form. The design finally evolved consisted essentially of a stressed skin trough section with all stiffening ribs on the outside. The particular designer in charge had previous aircraft experience and the influence of this can be clearly seen in Fig. 36 from which also the economy and cleanliness of the construction can be appreciated.

SOME INFLUENCES OF WELDING IN THE STRUCTURAL ENGINEERING FIELD

We would now draw the reader's attention to a trend which he may already have noticed from the various examples illustrating the main theme of this paper.

Widening Horizons

In the past the structural engineering industry in Europe has been confined to a rather narrow field and has become highly specialized. Now the process appears to be going into reverse. The structural engineer is becoming more and more intimately linked with the plant engineer. As welding leads more to the use of plates and tubes and of fabrication processes such as gas cutting, cold forming, etc., the structural fabricator is drawn into other fields such as fabrication of large vessels and certain plant parts. Thus we may expect the structural designer's horizons to widen and he may follow to some extent in the footsteps of his brothers in the shipbuilding field or in the aircraft field. After all, the hulls of aircraft and ships are just as much structures as a building or a bridge. The tubular drag line boom, designed and fabricated by a mainly structural firm, the small stressed skin bridge and the increasing use of torsion bars in ordinary building structures illustrate this tendency.

Ready Sprayed Steel

This integrating process whereby the trend toward specialization seems to be reversing cuts both ways. For example, building structures are probably the most

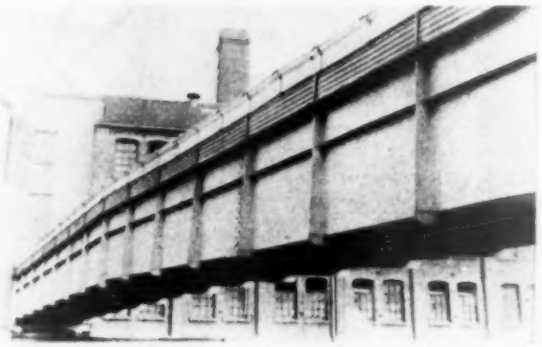


Fig. 36 Welded stressed skin bridge—Great Britain

neglected of all steel fabrications from the corrosion point of view and it is perhaps natural, therefore, that in this respect we might expect the structural engineer to lead. At the present time in Britain it is intended to set up a plant at one of the main steel mills for producing aluminum or zinc presprayed raw steel. Not only will this mark a profound step forward in tackling the corrosion problem in the field of building structures, but it is expected that naval architects, the manufacturers of air bottles, gasworks plant and so on will also take advantage of this product of the structural engineer's initiative.

Dangers—Brittle Fracture and Fatigue

Whenever evolution is rapid there are pitfalls to be avoided and it may not be out of place to indicate some of these here. The structural designer must take into account factors which in the past he has been able to neglect. For example, though the failure of certain bridges and certain welded oil tanks have been attributed to low-temperature brittle fracture, it was in the field of shipping that this problem was first clearly recognized and intensively investigated. We in Great Britain have countered this in the shipping field by the introduction of a rule known as Lloyds P.403 whereby the quality of ship plate from the low-temperature notch brittleness point of view is to some extent controlled. So far this measure has proved sufficient in combination with other design and quality control precautions to eliminate trouble. For special jobs special, low-tem-

perature tough steels, for instance Colvilles "Coltuf 28," have been evolved.

In the structural field as research reveals more of the nature and causes of brittle fracture and as development work produces new steels and so on, structural engineers are closely watching this potential danger. An example from the authors' practice may be quoted to illustrate this point. When in 1947 the heavy welded girders previously described were designed, the consensus of opinion in Great Britain was that there was little danger with British structural steel with its high manganese carbon ratio and particularly when related to British steelmaking practices. However, since that time there have been one or two failures involving such steel and, following on from the shipbuilding research mentioned, the properties of these steels have been further investigated. In the light of the latest knowledge it appears that in the thicker sections there exists some risk of failure with British structural steel and while the probability of catastrophe is very small it must now be taken into account. Thus in reconsidering the design of the heavy girders in connection with the extensions to the same building now being undertaken, the author has decided upon the substitution of "Coltuf 28" for mild steel in the flitch plates so that should a very adverse and unlikely combination of circumstances lead to brittle failure of the flange plates, the flitches will act as crack-arresting barriers and prevent catastrophic collapse.

As the structural engineer becomes involved in the design and fabrication of plant and so on he will have to take increasing notice of fatigue and other problems.

POSSIBLE FUTURE TRENDS

From the foregoing the reader will have gathered that in Europe there is a strong urge toward the achievement of the maximum efficiency and economy and in this the application of welding is playing a great part. It will be seen that the inevitable problems which arise are being tackled with energy and determination and also that the research work carried out in the universities and other research organizations is being energetically pressed into service. Welding in particular is exerting an integrating influence, it is widening the horizons and the scope of the structural engineer and increasing the contact and overlap between various branches of the engineering profession as a whole. In conclusion, therefore, it may be appropriate to attempt a brief survey of where welding is leading various people concerned.

The Steelmaker

We should start perhaps with the steelmaker. The demand for his materials may change; there will be heavier accent on plates and there will be a demand for new and different rolled sections. There will be a demand for new steels, high-tensile welding steels, notch-tough steels, presprayed steels and so on.

The Fabricator

The steelwork fabricator may expect to extend the scope of his activities to overlap, on the one hand, into the plant field and on the other, as he is already doing in Great Britain and in America, to the fabrication of ship subassemblies. But perhaps the most profound change must be in his approach to shop organization. It would seem that the urge toward greater efficiency will demand more and more mechanization—already automatic welding is playing a greater and greater part but to this it would seem that automatic handling must also be added giving production flow lines similar to those in other industries. Thus, perhaps, in Britain at any rate, we may see the welding shop of the future divided into two bays. In one all the cutting and preparation will be done, while in the other the materials completely prepared for welding will be assembled on the end of a continuous production line and will come out at the far end as a completed welded assembly; virtually all the welding being automatic. Perhaps this concept is colored slightly by Trade Union practices in Britain but we might expect something similar to occur in other European countries.

Though we have already indicated that the field of the structural fabricator may well be much widened by welding, this necessary drive toward more and more mechanization may result in individual fabricators becoming more specialized since the capital cost of the plant involved will be very much greater than at present, where much of the welding is still manual and there is very little mechanical handling in Europe. Thus we may see in a locality bounded by the transportation cost, one fabricator possibly specializing in welded plate girders, another in welded truss work, yet another in tubular constructions and so on. Following this train of thought the logical development in the field of building structures might be the concentration of labor or machine work on, for instance, the knees of portal frames, so that the completed framework consists of small but complicated units—forgings in some cases and welded assemblies in others—and plain rolled sections.

This latter idea might also lead toward more site fabrication; it might be possible for a specialist manufacturer to send the small complicated units to the site and for the plain sections to be delivered there from the mills and for fabrication and erection to take place simultaneously on the site. Indeed many designers in Great Britain believe that greater standardization in the fabricating shop linked together with greater freedom in site fabrication, as well as site erection by welding, are essential for economic progress. Further, the great flexibility of welding as a fabricating process may be taken advantage of by sending all units in a standard form to the site and then if individual cases are different for stairways, extra load points, and so on, these can be met by welding on or burning out material and suitably stiffening on the site prior to erection. Incidentally, this may also overcome one of the bugbears of the structural designer in that much of the data which he re-

quires for these odd special frames and so on is very rarely available until the last moment and much drawing office time is wasted on details which have later to be altered anyway.

The Structural Designer

Finally, we come to the professional engineer who is generally the designer of the structures. It seems to the author that he must bear the main burden of responsibility for ensuring that not only welding but also the allied fabricating and forming processes which logically go with it are utilized for the maximum benefit to society in general. From the examples which have been given in the paper it will be seen that in Europe there is a genuine urge toward new and better things in this field, but perhaps the author may be forgiven if in the concluding paragraphs he reverts to the position as he sees it in his own country. The application of welding and its associated processes is frankly lagging behind that which many designers had hoped for and indeed expected. There is often a tendency to blame this wholly on the fabricating firms—in Britain, most important design work is in the hands of Consulting Engineers—few of whom have as yet laid out their works to properly take advantage of the technological advances which have been made, particularly since World War II. Further, there is a tendency to blame the fabricators for pricing up welded jobs to delay the expensive obsolescence of many comparatively new riveting plants. Be this as it may, however, welding in the stuctural field is in the growing stage and the fabricator is not at all clear where welding is going. He gets a job in from one designer and the accent appears to be entirely on welded plate work, possibly with a tendency toward Vierendeel members. A little later he takes on another apparently similar construction for a designer and finds he is expected to turn out hundreds of tons of welded lattice work. Yet again another designer may come along requiring the structure to be almost entirely shop welded and erected by site bolting, another may come along expecting a certain amount of fabrication on the site, and an entirely site welded job and so on. With some justification the fabricator claims that he cannot be expected to scrap his existing layout and reorganize for fully automatic processes with automatic handling, etc. when it seems quite impossible for him to see the future design trends. Here perhaps the professional engineer is to some extent at fault, and it would seem that in Britain we must get through this development stage more quickly than the corresponding stage in riveted construction was encompassed.

There seems to the author to be a real need to get the fundamental lines upon which economic development is going to take place settled as quickly as possible. In this the professional associations, the government-sponsored research laboratories and the steel fabricating industry must collaborate. If it is not properly settled which is the economic form of structure, then perhaps some laboratory or theoretical or practical investigations may provide an answer very much quicker than the painful process of trial and error over several years.

When the broad lines of development have been agreed upon, it is then up to the professional engineer to pursue these lines; that is not to say that he should settle down to a formalized design and abandon the endless quest for the new and better, but that he must take advantage of any facilities, especially if very specialized, which the fabricating industry can offer. If, for example, a particular shop is set out for the fully automatic manufacture of plate girders, he might with advantage—and indeed with some economic gain—go for such work in a case which, perhaps on purely theoretical grounds, it might be argued that lattice work would be lighter. When this development takes place it would seem that the riveting era will be finally over and society generally will be able to reap not only the technical rewards but also the great economic gains which accrue from welding and its allied processes. Then we may look forward to further development and in this perhaps the greatest single factor will be the free dissemination of technical information and the closer and closer association between all branches of the engineering profession. The aircraft designer, naval architect, the automobile engine designer, the structural engineer, the bridge designer and so on, each with his different background and each with his different problems, can exert a stimulating and invigorating effect on his colleagues, and each has some experience to bring to the common pool of knowledge so that the experience of all may be enriched.

ACKNOWLEDGMENTS

In the preparation of this paper the author has drawn his information and illustrations from so many sources that a formal acknowledgment list would seem out of place. Some firms and individuals have been mentioned by name in the text. The author would now acknowledge and thank all the others who have contributed material to this paper.

Joining of Ductile Iron by Several Arc Welding Methods

Metal arc and inert arc welding of magnesium-containing cast iron, using nickel-iron electrodes, steel electrodes and nickel-iron filler wire

by T. E. Kihlgren and H. C. Waugh

INTRODUCTION

"DUCTILE IRON" is a relatively new engineering material which combines the castability and machinability of gray cast iron and the mechanical properties of cast steel to a remarkable degree. The material, sometimes referred to as "spheroidal-graphite cast iron," or "magnesium-containing cast iron" is characterized by a structure in which graphite is present as spheroids instead of the flake form found in gray cast iron. It is this structural feature which accounts largely for the unique characteristics of spheroidal graphite cast iron.

In many of the fields of application of spheroidal graphite cast iron, weldability is an important consideration. With the increasingly wide acceptance of Ductile Iron, the need for more information on its response to welding as a means of fabrication, repair and salvage has become progressively greater.

This paper deals with metal arc welding and to a lesser degree with inert arc welding of magnesium-containing cast iron, using nickel-iron electrodes and filler wire. Brief data are also presented on metal arc welding of this material with steel electrodes.

COMPOSITION AND PROPERTIES OF UNWELDED DUCTILE IRON

Some comment may be in order on the composition and properties of magnesium-containing cast irons. Basically, Ductile Iron is a gray cast iron which has been treated with magnesium, the residual magnesium preferably being of the order of 0.05–0.08%. The base composition can be further strengthened by increases in manganese and nickel content. The following tabulation represents a satisfactory range of compositions of Ductile Iron, unwelded:

C	3.2/4.2
Si	1.0/4.0
Mn	0.1/0.8
P, max.	0.10
Ni	0/3.5
Mg	0.05/0.10

The mechanical properties, depending upon whether the casting is in the as-cast or annealed condition will range as shown in Table 1. Using certain compositions and appropriate heat treatments involving quenching and drawing, tensile strengths as high as 150,000 psi with 9% elongation have been obtained.

Table 1

Tensile strength, psi	60,000–105,000
Yield strength, psi	45,000–75,000
Elongation, %	1–20
Bhn	140–290

The greater portion of current production of Ductile Iron castings would fall within the following range of composition and properties shown in Table 2. Most castings are used in either the as-cast or annealed condition, the anneal normally consisting of 1 hr at 1650° F followed by slow cooling to 1275° F, holding for 5 hr and air cooling. In the paper, the above condition will be designated as annealed or FHT.

Table 2

Composition range	*Properties*
3.2–3.8 C	60/95,000 psi tensile strength
1.75–2.75 Si	45/65,000 psi yield strength
0.20–0.80 Mn	3/20% elongation
0–1.0 Ni	150/250 Bhn
0.05–0.08 Mg	
0.10 P, max	
0.02 S, max	

Most of the data to be presented will refer to base plate falling within the range of composition of Table 1. Generally, low-alloy plate containing less than 0.35 Mn and 0.75 Ni was used for the welding tests.

Part I. Metal Arc Welding

METAL ARC WELDING WITH NICKEL-IRON ELECTRODES

A considerable amount of experience has been obtained on welding of Ductile Iron with coated austenitic nickel-iron electrodes. The particular electrode used for these studies is designated commercially as Ni-Rod 55 and is essentially a 60/40 nickel-iron core wire, coated with a carboniferous lime-spar type of flux coating. There are several other commercially available nickel-iron electrodes differing primarily in the composition of the flux coating applied. Much of the following recommendations on welding procedure will apply to these electrodes also. Since the coating is an important consideration, it is obvious that the data on weld cracking, mechanical properties, etc., cannot be applied indiscriminately to all electrodes of this general description. In the succeeding portions of the paper, when the term "nickel-iron" electrode is used, the data will be based on Ni-Rod 55 experience. In using other commercial electrodes of this type, the electrode manufacturer's instructions with respect to electrode manipulation, welding currents and welding procedure should be consulted.

CHOICE OF CASTING COMPOSITION FOR WELDING

In general, the higher the manganese

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and nickel contents (and hence the weld hardenability) the less latitude there is in welding and the greater will be the need for preheat. For example, a Ductile Iron containing 0.90 Mn and 1.5 Ni will require more care in welding than one with 0.25 Mn and 0.70 Ni, other things being the same. The increase in weld hardenability of the former will be reflected in greater machining difficulties in the heat-affected zone. It is also important to control phosphorus content, if weld cracking is to be avoided. A 0.10% P maximum appears satisfactory when plates are welded with nickel-iron electrodes or filler wire. It is believed that a 0.05% maximum may provide further latitude and is probably necessary where mild steel electrodes are to be used. In general, in applications where weldability is a prime consideration, the composition range of Table 2 should be further narrowed with respect to manganese, nickel and possibly phosphorus along the lines indicated above. The following nominal composition, for example, would be expected to show a good combination of mechanical properties and weldability using several welding processes: 3.50 C, 2.40 Si, 0.25 Mn, 0.70 Ni, <0.05 P, 0.06 Mg.

EFFECT OF PLATE CONDITION

For maximum welding response, Ductile Iron castings should be welded in the annealed (FHT) condition. This treatment insures the greatest freedom from fusion line cracking hazards, as well as the best machining qualities, at least as far as as-welded joints are concerned. Good quality joints can be obtained on either as-cast or annealed material using nickel-iron electrodes or filler wire, but when using mild steel electrodes, the prior anneal is necessary, as will be discussed later.

ARC-WELDING PROCEDURE WITH NICKEL-IRON ELECTRODES

The following procedure is suggested, much of it being applicable to welding with other electrodes and processes.

Preparation of Castings

This phase of any welding job is important, and successful welding may hinge upon the care taken at this stage. On new castings, the preparation consists mainly of removing the casting skin and adhering foreign matter from the surfaces to be joined and from the immediately adjacent area. This can be done by machining, grinding, shot blasting or other suitable means. Such a precaution not only decreases danger of contamination but also insures better slag coverage and removal characteristics.

Castings which have been in service and require weld repairs may be found to be oil impregnated. Such castings are degreased by use of a solvent, such as carbon tetrachloride, sometimes in conjunction with cleaning by steam, which will serve to remove surface contamination with oil and grease. In some instances, oil may have penetrated the casting sufficiently so that torch heating to about 750° F may be indicated to decompose oil and grease and drive off volatile matter.

Welding Amperages

The nickel-iron electrodes under discussion operate on dc or ac. When used on dc, reverse polarity is recommended. The welding current should be as low as possible, consistent with smooth operation, good bead contour and the obtaining of a good "wash." Recommended ranges are given in Table 3.

Table 3

Electrode	*DC*	*AC*
3/32	50–80	60–90
1/8	90–110	100–120
5/32	120–140	130–150
3/16	140–160	150–170

Electrode Manipulation and Welding Sequence

In general, the electrode should be operated with a normal weave, and conventional width beads deposited. The stringer bead technique often used in cast-iron welding is not recommended with the nickel-iron electrode. In making butt joints, a 60- to 75-deg vee should be employed. Where a considerable amount of metal must be deposited, as in filling in of a large cavity, the sides may be "buttered" and the cavity filled in gradually toward the center of the repair area. In any event, the welding should be staggered so that the heat is distributed reasonably uniformly through the weldment.

Peening

This is often done to reduce the stresses in the joint. If peening is to be done, it should be by repeated moderate blows rather than by a few heavy blows, peening each bead as deposited. A 1/2–3/4-in. diam rounded peening tool or a ball-peen hammer is suitable. Peening, properly done, can be quite effective in reduction of stresses. A 6- x 6- x 1/2-in. joint, unpeened and unrestrained, exhibits considerable bowing. The same joint, suitably peened, can be completed with virtually no bowing.

Preheating

In no instance should welding be done on cold castings, as might occur in unheated areas in cold weather, and a preheat of at least 150–200° F should be applied under these conditions. Low-alloy Ductile Iron in the annealed condition may be welded without preheat in many instances. The higher the alloy content (and hardenability) or the greater the restraint, the greater is the need for preheat. For a given composition, as-cast material will be more apt to need preheat than annealed material. In any event a preheat is never harmful, and is a good precaution to observe in order to minimize danger of fusion line cracking. Obviously the amount of metal to be deposited and the severity of subsequent service conditions of the weldment must be considered in deciding upon the use and the amount of preheat. While a preheat of 300–350° F is often adequate, one of 550–600° F is preferred where high stresses are to be expected. An added advantage of preheat is the greater facility of obtaining a good bead contour and a good wash.

Continuous Welding vs. Intermittent

In welding of cast iron, intermittent welding is frequently used, a method in which 2 or 3 in. of weld are deposited, the bead allowed to cool until just warm to the touch, another short bead deposited, and so on. Using nickel-iron electrodes, this procedure is not usually employed in welding either cast iron or Ductile Iron. Continuous welding is used, as in normal welding practice, the heat input of the early passes serving to supply a moderate preheat. However, the bead sequence should still call for a staggering procedure to get reasonably uniform distribution of heat in all parts of the weldment.

Position Operability

The particular nickel-iron electrode used is operable in vertical and overhead as well as in downhand positions. Figures 1–3 show 1/2- x 6- x 6-in. butt welds in all positions, partially completed so that the contour of intermediate passes may be seen. When operating out of position, a smaller electrode and lower amperages should be used than for flat welding, as is customarily the case in welding other metals. For example, in Figs 2 and 3 a 1/8-in. electrode at about 105 amp was used, while the downhand weld was made with 5/32-in. electrode at 135–140 amp. A 1/8-in. electrode, as used in a down-hand weld on 1/2-in. plate, would be operated at about 110–115 amp.

PROPERTIES OF NICKEL-IRON ARC-WELDED JOINTS

The data shown below are averages of two groups of tests, based on standard 0.505-in. and subsize 0.357-in. round tensiles machined from butt welds in 1-in. and 1/2-in. plate, respectively. Welds

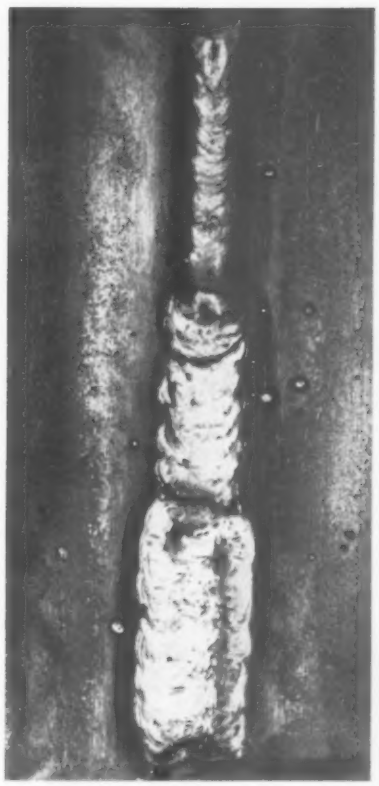


Fig. 1 Flat butt weld in annealed 1/2-in. Ductile Iron plate. Welded with nickel-iron electrode

Fig. 2 Vertical butt weld in 1/2-in. plate of annealed Ductile Iron using the nickel-iron electrode

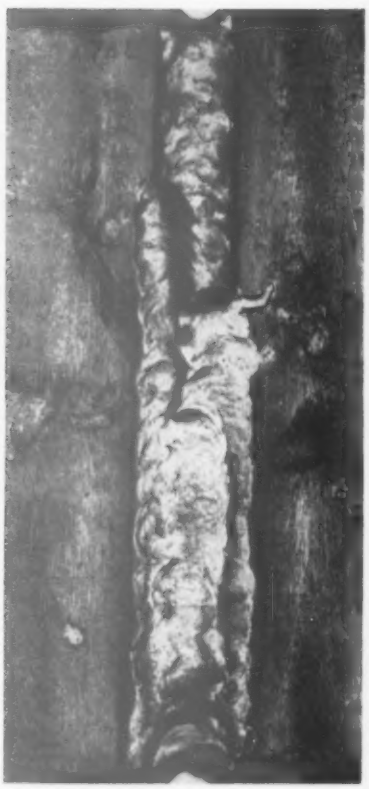


Fig. 3 Overhead butt weld in 1/2-in. as-cast Ductile Iron plate using the nickel-iron electrode

without preheat and with a 600° F preheat are grouped together since no important differences were consistently present. The base plate contained about 3.30 C, 2.30 Si, 0.70 Ni, 0.35 Mn, 0.05 Mg, <0.05 P. Welds were made with 5/32-in. diam nickel-iron electrodes at about 130–140 amp, d-c, reverse polarity. In the case of the welds in 1/2-in. plate, the welded joints were 6 x 6 x 1/2 in. and in the 1-in. plate, 5 x 10 x 1 in. A double-vee, 70-deg included angle, a 1/8-in. root gap and a copper backing were used. The welds were quite severely restrained with four C clamps and were unpeened.

The data illustrate the value of welding the plate in the annealed (FHT) condition, such joints exhibiting fairly good ductility without a postweld heat treatment. The effect of the postweld full anneal on Ductile Iron welded in the as-cast condition is of course marked, with respect to ductility. A 1275° F treatment applied directly to the as-welded joint without the prior 1650° F treatment was also helpful and developed similar properties to the FHT specimens in the one set of tests made.

Figure 4 is a photograph of a set of tensile bars showing location of fracture and the properties obtained on the particular specimens illustrated.

Table 4—Average Mechanical Properties of Nickel-Iron Arc-Welded Joints in Ductile Iron

Plate condition	*Postweld, HT**	*Yield strength, psi, 0.2% offset*	*Tensile strength, psi*	*Elongation in 2 in.*
As-cast	AW	56,900	65,000	1.5
As-cast	FHT	45,900	63,800	10
As-cast	FT	47,700	61,600	5
Annealed (FHT)	AW	44,500	60,600	9
Annealed (FHT)	FHT	42,500	60,500	12
Annealed (FHT)	FT	44,700	60,700	13

* AW = as = welded; FHT = 1650F-1-FC + 1275-5-AC; FT = 1275F-5-AC.

HARDNESS OF NICKEL-IRON ARC-WELDED JOINTS

The nickel-iron electrode deposits weld metal having a Vhn of about 160, in the absence of plate dilution. The as-welded hardness of the weld metal, when diluted with the ductile iron base metal, is well above this figure and varies considerably, depending on the extent of dilution. In general, the weld metal of a multilayer single-vee butt joint in 1/2-in. ductile iron plate will exhibit about 225 Vhn in the as-welded condition.

In single-layer welds, the weld hardness can be kept at a satisfactorily low level simply by use of moderately heavy beads. For example, a light bead deposited upon a cast-iron plate, using a rather rapid speed of travel, may show hardnesses of the order of 350–380 Vhn due to excessive plate dilution. By depositing a substantially heavier (but not abnormally so) bead at the same amperage, using a slower welding speed, the increased ratio of electrode metal to base metal results in the much lower hardness range of 180–225 Vhn. Similar effects are experienced in ductile iron welding.

In operations where single-bead welds are to be made, the use of the so-called heavy-pass technique can be quite helpful where moderately low weld metal hardnesses are

desired. In multilayer welding, the technique is not required for the above reason, but it has been found useful, especially in the root passes of the weld, in providing further insurance against weld hot cracking in welding heavy sections and is quite generally employed.

Hardness traverses were made on a set of Ductile Iron joints produced with nickel-iron electrode on a Ductile Iron of the following composition: 3.67 TC, 2.35 Si, 0.23 Mn, 0.73 Ni, 0.055 Mg, <0.05 P and 0.37 Cu. The results are summarized in the following tabulation:

Table 5—Condensed Hardness Data, Vhn (20 Kg)

				HAZ		
Test	*Sample*	*Plate*	*Weld*	*Max*	*Avg*	*Width, in.*
1	As-cast plate, as-welded	265	227	750	429	0.093
2	Annealed plate, as-welded	173	229	665	449	0.063
3	Welded joints, annealed	165	184	335	210	0.015

The high hardness shown for the maximum in the heat-affected zone (HAZ) of tests 1 and 2 was obtained at the edge of fusion, and was confined to a very narrow zone. It reflects the presence of the carbide flash and martensite in this location. The maximum HAZ value for test condition 3 is higher than might be expected after a postweld FHT anneal. Some modification of the FHT heat treatment would be indicated for this heat of plates if further softening were desired. However, both shaper and drilling and tapping operations showed no signs of difficulty in the heat-affected zone, suggesting the FHT treatment to have produced adequate softening. The 335 Vhn value of the table for test condition No. 3 represented an extremely narrow zone, the average HAZ hardness being of the order of 210 Vhn.

The width figures shown in the table are of course approximate but reveal the somewhat narrower heat-affected zone of the as-welded joint made on annealed plate compared to that on as-cast plate, and the very narrow HAZ of the annealed joints.

The tabulation shown above represents a series of traverses on one lot of cast plates of the analysis shown. It is of course obvious that the HAZ values of the as-welded joints will vary considerably with casting composition. Elements such as manganese and nickel will be expected to increase the extent and hardness of the heat-affected zone while elements such as silicon will be expected to decrease weld hardenability. For example, a hardness survey on a welded joint made on annealed centrifugally cast Ductile Iron pipe containing 3.6 C, 2.7 Si, 0.3 Mn, 0.4 Ni and 0.05 Mg showed a maximum of 400 Vhn in the heat-affected zone, and an average value of 340 Vhn in this area.

In addition to plate casting composition, it should be recognized that any factors affecting the cooling rate such as preheat, speed of welding, heat input during welding, etc., will influence the HAZ hardness ranges.

STRUCTURE OF NICKEL-IRON ARC WELDS IN DUCTILE IRON

The weld structure contains graphite in an austenitic matrix, with some carbides co-present in cored areas. Annealing treatments result in further rejection of graphite and decomposition of any iron carbides which may be present. A carbide flash is always present at the frontier of fusion, with the adjacent heat-affected zone of the cast Ductile Iron showing considerable martensite. In multilayer welding, the heat of subsequent passes exerts an annealing effect adjacent to at least a portion of the previous weld layer. This annealing effect serves to temper the martensite and, depending upon temperature, may also decompose some of the primary carbides, yielding ferrite and small spheroids of graphite.

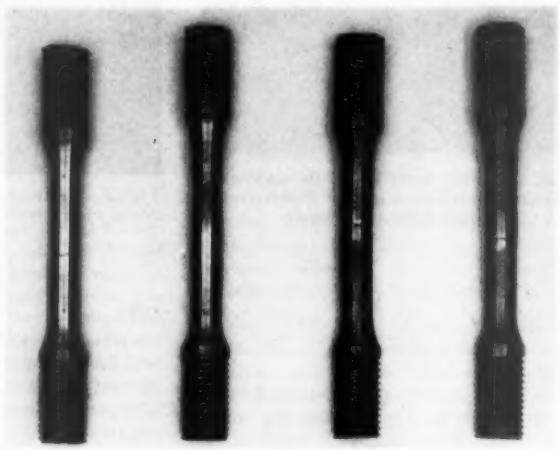
After a full anneal of the welded joint, the heat-affected zone shows mainly a ferrite matrix with spheroidal graphite. These spheroids are rather small and often quite closely spaced, interfering somewhat with obtaining of maximum ductility.

MACHINABILITY OF NICKEL-IRON ARC WELDS IN DUCTILE IRON

In general, the weld metal offers no problem in machining, except possibly under conditions of heavy plate dilution. The ease of machining of the joint is, rather, determined by the condition of the plate in the fusion zone and heat-affected zone. The structural characteristics giving rise to machining difficulties in this area have already been discussed. The carbide flash is quite narrow, usually 0.005–0.015 in. in width. The over-all width of the heat-affected zone varies, but a width of the order of 0.10 in. is common.

It has been previously mentioned that the extent and hardness of the heat-affected zone in the as-welded joint is a function of both the plate composition, heat input and cooling rate following welding. The higher the weld hardenability of the casting composition, and the faster the cooling rate, the greater will be the adverse effect on machinability, other things being the same. It is also apparent that, in multilayer welding, some amelioration of the condition in this hard zone can be effected as a result of the tempering effect of succeeding passes.

In the following illustrations, which are based on shaper machining tests on $\frac{1}{2}$-in. thick ductile iron welded plate, containing about 3.60 TC, 2.40 Si, 0.30 Mn, 0.70 Ni, 0.05 Mg and <0.05 P, some of the factors influencing machinability will be demonstrated.



Weld no.	4692	4696	4693	4697
Plate cond.	As-cast	FHT	As-cast	FHT
Preheat	None	None	None	None
Postheat	None	None	FHT	FHT
Tensile strength, psi	71,500	63,500	63,500	59,750
Yield strength, psi	59,750	45,400	45,800	42,100
Elongation % in 2 in.	3	15	10	13

FHT = 1675° F.—1 hr—FC to 1275—5 hr—AC

Figure 4

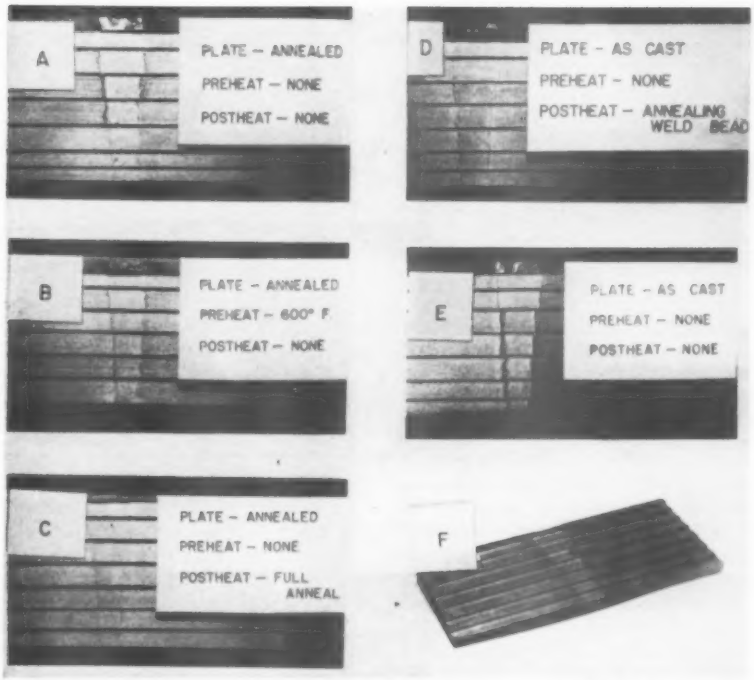


Fig. 5 Shaper machining tests on Ductile Iron arc-welded joints

The type of machining test specimen used is shown in Fig. 5*F*, the specimen, measuring 6 x 3 x ½ in., being cut out of a 6 x 6 x ½-in. welded joint.

The machining test was carried out in the shaper using the following conditions:

Speed, strokes per minute	16
Depth of cut, in.	0.050
Feed, in. per stroke	0.01
Cutting tool bit: High-speed tool steel (Rex 95)	

Each cut was started at the end of the weld and continued until all but ⅜ in. of the previous cut had been removed. The first cut was a reference surface cut, sufficient to remove casting skin. The balance of the cuts were 0.050 in. deep. The tool bit was resharpened before each cut except in the joints fully annealed after welding.

Figure 5*A* demonstrates the appreciably better machining response of as-welded joints on annealed plate (no cover pass). In the as-cast plate, Fig. 5*E*, tool breakdown frequently occurred even in making a single cut. The joint on annealed plate, while still showing a hard zone, could be machined without too much difficulty. By using a 600° F preheat and welding continuously, a substantial improvement can be obtained. This is particularly noticeable in welding annealed plate (*B* vs. *A*), *B* showing very little difficulty in the heat-affected zone. The effect was less noticeable in welds on as-cast plate since a pronounced hard zone persisted even with the preheat, but tool breakdown was much less of a problem. If a full anneal (1650 + 1275° F) is applied after welding, it seems to make little difference whether the weld has been made on as-cast or annealed plate, or whether or not a preheat or other special techniques have been employed. In machining the fully annealed joint, all the steps were cut without resharpening of the tool bit (Fig. 5*C*). A hole was drilled at the interface and tapped without difficulty as may be seen in Fig. 5*F*.

There is some indication that relatively low tempering treatments can produce fairly good machinability. Figure 6 shows two sections from the same joint, one portion of which has been torch annealed for 10–15 min at about 1000° F. The two cuts on each sample are 0.050 and 0.100 in. below the reference surface. The beneficial effect of the brief torch anneal is apparent. It would be expected that a 1275° F anneal (without the prior 1650° F treatment) might offer adequate machinability for some operations.

The annealing cover pass technique can also be usefully employed, but it is best applied while the joint is still hot. Figure 5*D* shows a joint made by continuous welding, a full weave annealing cover pass being applied, confining the latter to the weld proper, without remelting parent metal. Flush welds (unless a postweld heat treatment is used) are apt to be troublesome in subsequently making surface cuts. A weld reinforcement applied as a cover pass not only strengthens the joint but also tempers the hard heat-affected zone occurring as a result of depositing the flush beads (Fig. 7). The value of using a tungsten carbide tipped tool is shown in Fig. 8, the ridge effect being much less pronounced when this type of tool is used.

Summarizing the observations on machinability of welded joints in Ductile Iron, as measured by shaper tests:

1. The higher the nickel and especially the manganese content, the higher will be the weld hardenability and hence the poorer the machinability in the heat-

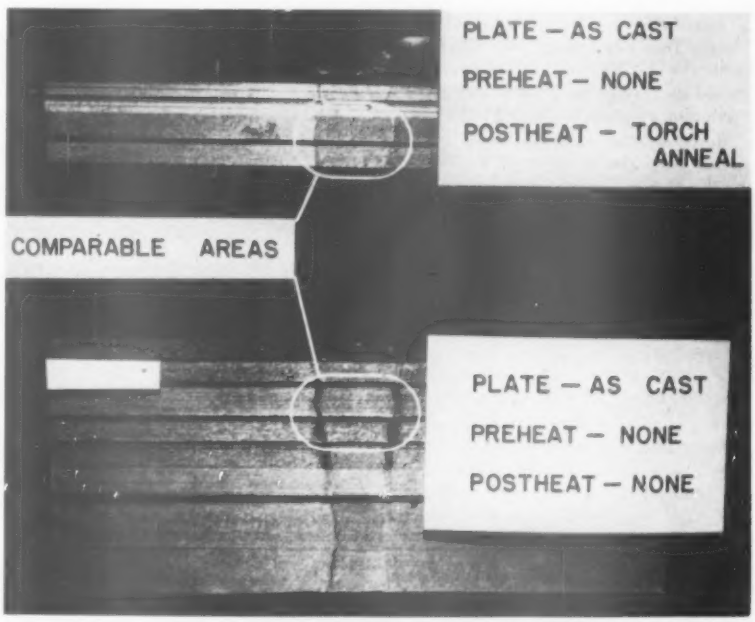


Fig. 6 Effect of a postweld torch anneal on the machinability of Ductile Iron joints

affected zone in as-welded joints. Other alloying elements which increase weld hardenability, if present, will also be expected to decrease machinability in the heat-affected zone.

2. Joints made on plate annealed before welding are more readily machined than those on as-cast material.

3. A full anneal after welding may be expected to provide quite good machinability under wide variations of composition, prior plate condition and welding procedure.

4. A relatively low temperature postweld anneal at 1275° F will improve machinability considerably, often to a satisfactory extent.

5. A simple torch-applied postweld anneal at about 1000° F for 10–15 min ($^1/_2$-in. thick section) may provide adequate machinability for a number of operations.

6. When a measure of machinability is needed in the as-welded condition, and postweld treatments are not feasible, recourse to continuous welding, with a final cover pass, properly applied, may often suffice, when used in conjunction with a suitably chosen casting composition.

7. Carbide tipped tools offer advantages over tool steel bits, particularly where a substantial hard zone exists.

8. Where a quite high degree of machinability is a prime consideration, a full anneal should be given the welded Ductile Iron casting, prior to machining.

It is recognized that shaper operations are much less demanding than some of the other machining processes, such as lathe operations, for example, and the above observations must be considered specifically as applied to the former. It is reasonable to assume, however, that Ductile Iron joints made with nickel-iron electrodes and fully annealed after welding, should be readily machinable using most machining methods, including lathe work.

However, the data developed in the shaper tests should be used, in general, for guidance, in indicating trends in a qualitative way.

DISSIMILAR METAL JOINTS

In most types of fabrication it is often necessary for joints to be made between different materials. So too with Ductile Iron, the necessity frequently arises for it to be joined to some other material. Such a joint is readily made by metal arc welding using a nickel-iron electrode.

In Fig. 9 are shown welds wherein Ductile Iron is joined to each of five different materials: mild steel, Type 316 stainless steel, Monel, nickel and Inconel. The welded joints were 6 x 6 x $^1/_2$ in., each of these joints being made by continuous welding of annealed Ductile Iron plate to the dissimilar metal. A $^5/_{32}$-in. diam nickel-iron electrode was used at 135–140

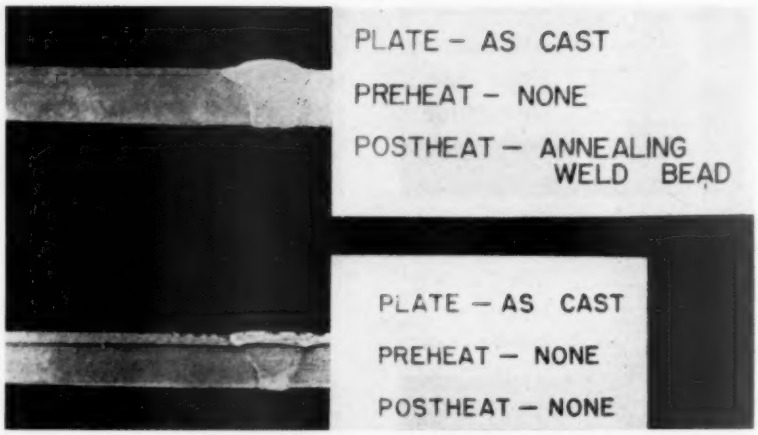


Fig. 7 Machinability of reinforced weld vs. flush weld

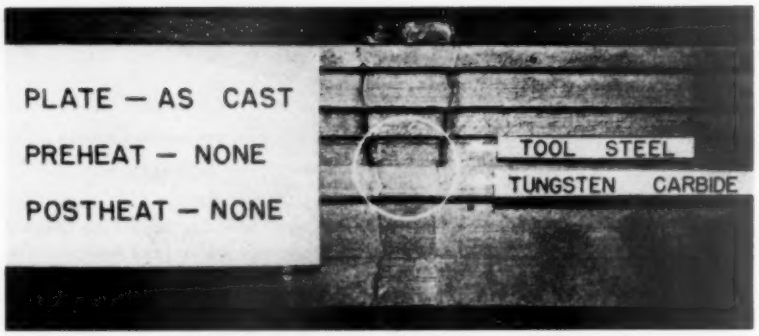


Fig. 8 Tool steel vs. tungsten carbide tools for machining Ductile Iron, as-welded

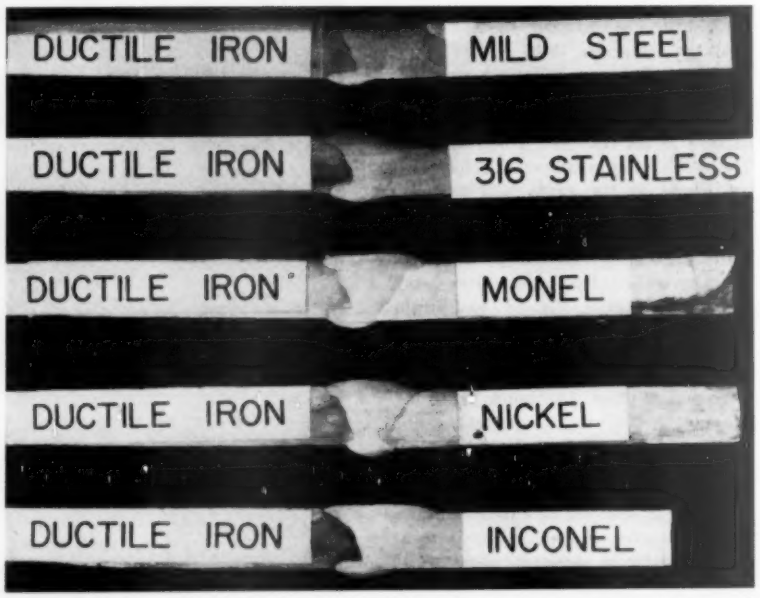


Fig. 9 Joints between $^1/_2$-in. annealed Ductile Iron plate and other metals welded with nickel-iron electrodes

Fig. 10 Ductile Iron-to-steel weld in Hydrafuge part made with nickel-iron electrode

amp, the single-vee joint being completed with 4 passes and a sealing bead on the root side. Neither preheat nor postheat was employed for these welds. While the plates were quite severely restrained during welding, the resulting welds were free of any indications of cracking.

A great many applications involve the joining of Ductile Iron and mild steel. In Fig. 10 is shown a Ductile Iron casting welded to a steel pipe. This weldment is part of a Hydrafuge, a machine used in the pulp and paper industry. The weldability of Ductile Iron was an important factor in its selection. A nickel-iron electrode was used for this joint.

Another application involving Ductile Iron welded to mild steel is illustrated in Fig. 11. Here is shown an hydraulic farm-loader part where a cold punched Ductile Iron clevis cap is joined to a mild steel tube.

To sum up the welding of Ductile Iron to steel, it can be said that joining these two materials by welding when using a nickel-iron electrode is even more easily accomplished than welding ductile iron to itself, since the heat-affected zone of the mild steel remains soft and can yield to relieve stress, while in the matched welds, a hard brittle zone exists on both sides of the joint in the as-welded condition.

FIELD APPLICATIONS OF NICKEL-IRON ELECTRODE

Large quantities of nickel-iron electrodes sold under several trade names have been consumed in welding gray iron castings, and many case histories are available. Due to the comparative newness of Ductile Iron, the field experience with Ni-Rod 55 in the welding of this material is, of course, more limited, but a few applications can be presented. Figure 12 shows a Ductile Iron screw for a dehydrator used in the paper mill industry. The flutes of this screw are regularly overlaid using nickel-iron electrodes, in order to obtain closer clearances in the screw housing and to cut down wear. The leading face and the rim of the flutes are built up about $^1/_8$ and $^1/_4$ in., respectively, using a 200° F preheat and continuous welding. Welds are peened with a round nose tool and a pneumatic hammer.

Figure 13 shows some lengths of centrifugally cast Ductile Iron pipe welded in the annealed condition using nickel-iron electrodes. As will be seen, failure occurred in hydrostatic bursting tests at about 5000–6000 psi through the pipe wall, and a considerable distance from the weld.

Figures 10 and 11 involving field applications where Ductile Iron-to-steel joints are required have already been discussed and need no further comment.

METAL ARC WELDING WITH STEEL ELECTRODES

Field experience involving welding of Ductile Iron with steel electrodes has been scanty, and weld cracking has generally been present. The results of laboratory tests suggests that a mild steel electrode of the low-hydrogen type may be used under certain conditions. The limitations

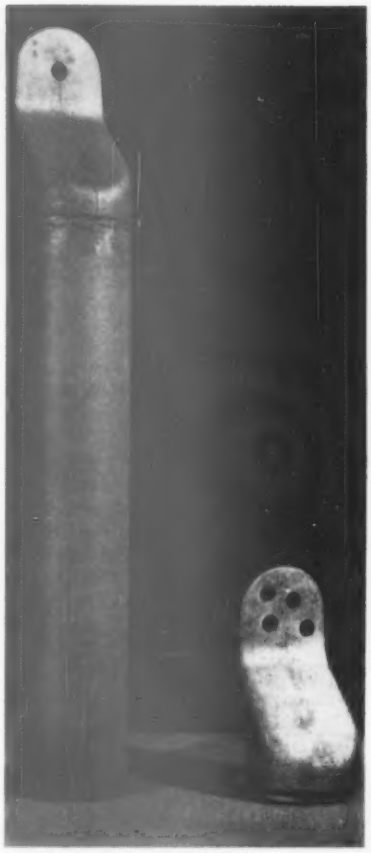


Fig. 11 Ductile Iron clevis cap, arc welded to steel tubular part

which must be recognized in order to provide a reasonable chance of success are:

1. Castings should be relatively low in manganese and nickel. The maximum permissible levels have not been established, but good results have been obtained on plates containing less than about 0.30–0.35 Mn and about 0.70 Ni.
2. Castings should be low in phospho-

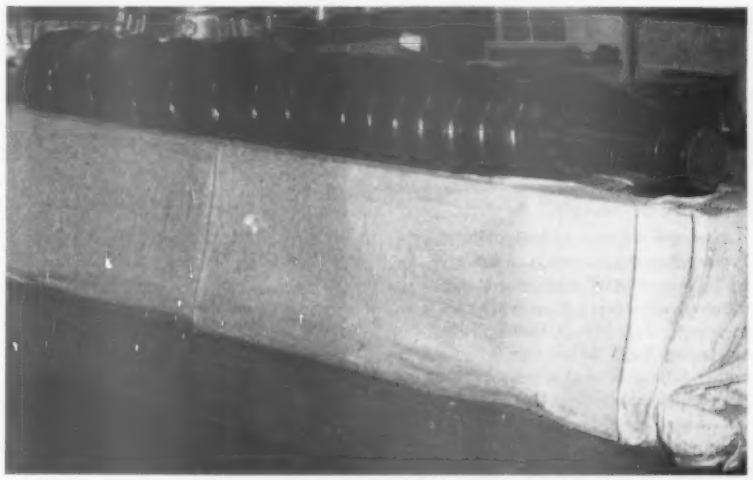


Fig. 12 Ductile Iron screw with flutes overlaid using nickel-iron electrode

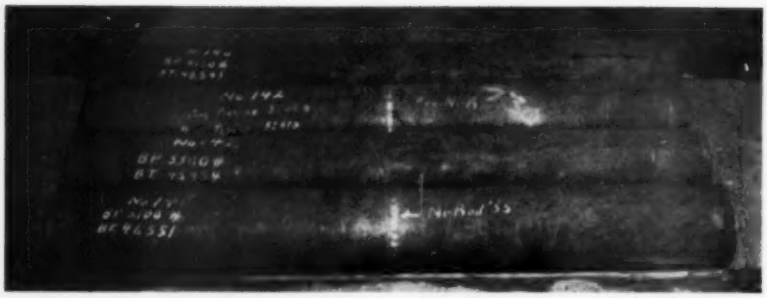


Fig. 13 Centrifugally cast Ductile Iron pipe, butt welded using nickel-iron electrode

rus, at least below about 0.05% and still lower if possible. This must be accomplished by proper selection of raw materials.

3. Castings must be in the fully annealed condition prior to welding.

The tolerance for sulfur in the base composition has not been ascertained but magnesium-treated irons are customarily low in this element and it was not a factor of concern in the present studies.

Under the above conditions of composition and heat treatment, butt-welded plates $^1/_2$ x 6 x 6 in. have been successfully produced under moderate restraint both with and without a 600° F preheat. However, cracking tendencies were observed in unpreheated welds in some instances, and the use of a 600° F preheat is advisable to provide a greater margin of safety. The commercial electrode used, though possessing quite good operating and slag characteristics on wrought steel, offers considerable difficulty with slag interference when applied to Ductile Iron. Whether the behavior is characteristic of this class of electrode or applies to the particular one used has not been definitely determined, but it is believed that considerable variations can be expected in this respect in electrodes of the low-hydrogen type obtained from various sources. Despite these operating difficulties, however, joints were successfully made on plate of the following composition: 3.68 TC, 2.34 Si, 0.23 Mn, 0.73 Ni, 0.055 Mg, <0.05 P, 0.005 S balance Fe. Tensile tests on subsize 0.357-in. diam transverse tensile test specimens showed the following range of properties: 42/47,000 psi yield strength (0.2% offset), 55/65,000 psi tensile strength and 5–9% elongation. In most instances, failure occurred near the plate-weld junction.

Attempts to weld as-cast plate of preferred composition were unsuccessful unless a high (1000° F) preheat was used and even then the physical properties were mediocre. Likewise, a Ductile Iron heat containing 1.6% nickel and 0.70% manganese exhibited weld cracking, occurring in this instance in the annealed as well as the as-cast condition. Ductile Iron containing 0.10% phosphorus, though readily welded with nickel-iron electrodes, yielded cracked welds using the steel electrode.

The field of application of steel electrodes in welding Ductile Iron is obviously much more limited than with the nickel-iron type and under the optimum conditions for their use, some weld cracking hazards are still present. Whenever steel electrodes are used, suitable exploratory tests should be made to determine if the conditions of plate composition, heat treatment and the quality of weld required will permit their use. It would be expected that steel electrodes can be more readily used to join Ductile Iron to mild steel than to itself.

Part II

OTHER WELDING PROCESSES

Ductile Iron can be welded successfully by other processes than metal arc, some of which will be discussed in the ensuing sections.

INERT-GAS-SHIELDED METAL-ARC WELDING

This generic term is cumbersome and the process is more often designated by the trade names Aircomatic or Sigma. For convenience, it will be referred to in the discussion, somewhat inaccurately but briefly, as "inert metal arc" and its counterpart with the nonconsumable electrode as "inert tungsten arc."

Inert Metal Arc: Nickel-Iron Filler Wire

This method of welding is readily accomplished using a 60/40 type nickel-iron filler wire. The wire is identical in composition with the core wire used in the nickel-iron electrode previously discussed.

Uncracked welds have been obtained readily in Ductile Iron of the preferred composition in sections up to 1 in. in thickness, without preheat or with preheats up

Table 6

Filler wire	60/40 Ni-Fe alloy
Wire diameter	0 045 in.
Wire feed	435 ipm
Welding current	170 amp, d c
Polarity	Reverse
Backing	Carbon block or copper
Shielding gas	argon
Gas flow	20 to 40 cfh
Preheat	None to 600° F (depending on job; higher preheats quite acceptable, but usually unnecessary

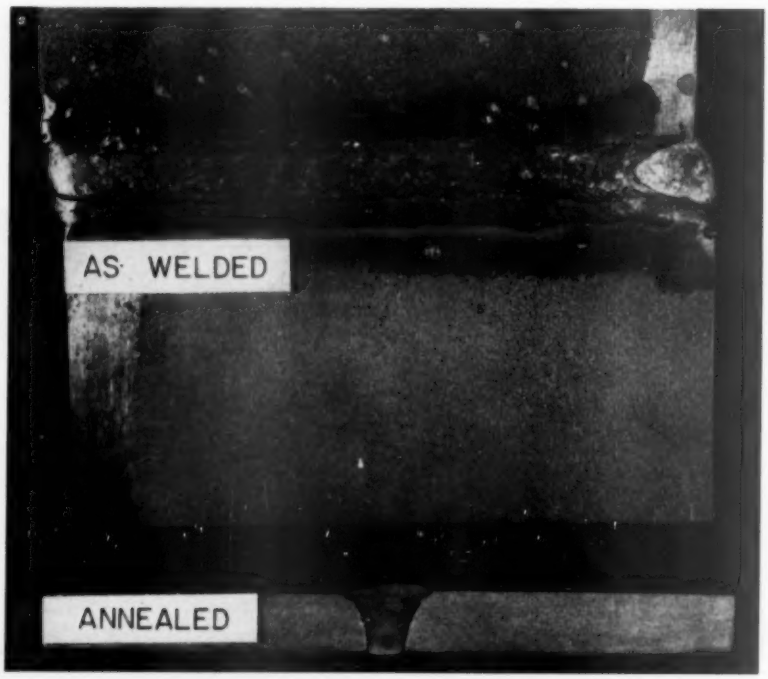


Fig. 14 Inert-gas metal-arc weld using nickel-iron filler wire

to 1000° F, using the inert-gas metal-arc process.

In making joints in $\frac{1}{2}$- to 1-in. thick Ductile Iron, the conditions given in Table 6 worked well.

Welds of quite good quality can be obtained with 20 cfh gas flow, but 30–40 cfh is preferred to provide greater protection against gas porosity. There is some indication that a carbon-block backing gives better protection than a copper backing.

Figure 14 shows a butt-welded joint made without preheat on as-cast Ductile Iron plate of preferred composition using the welding conditions tabulated above. A typical cross section of such a weld, after a postweld anneal (FHT) is depicted in the same figure. Good results have been obtained both with a single-vee and a double-vee or X-joint design. A number of joints were made on Ductile Iron plate containing 0.02, 0.06 and 0.10% phosphorus to examine the effect of this element, with no indications of weld or fusion zone cracking at any of the three levels.

Limited tensile test data suggest that inert metal-arc joints in Ductile Iron, made with preheats up to 600° F, using nickel-iron filler, may be somewhat inferior in mechanical properties to joints metal-arc welded with nickel-iron electrodes. For example, a joint in 1-in. plate, welded in the as-cast condition using the welding procedure of Table 6 and subsequently given a postweld full anneal yielded: 44,000 psi yield strength (.2%), 55,000 psi tensile strength and 6% elongation. The average properties of Table 4 show about 64,000 psi tensile strength and 9% elongation for this condition in metal-arc welds, with some individual tests in the latter falling to about 60,000 psi with 7% elongation.

Some tensile data were also obtained on inert metal-arc welded joints made on annealed plate of preferred composition using a 1000° F preheat and a 750° F postheat and slow cool immediately after welding. The welds were made on $\frac{1}{2}$-in. thick annealed Ductile Iron plate. The average tensile test data obtained with the nickel-iron filler are shown below:

Table 7—Inert Metal-Arc Welds,* Nickel-Iron Filler

Post Weld, HT	*Yield strength (0.2%), psi*	*Tensile strength, psi*	*Elongation % in 2 in.*
As-welded	44,750	62,500	5
FHT	42,400	62,700	17

* 1000° F preheat, followed by slow cool from 750° F; annealed plate.

The strength properties are similar to those shown for unpreheated metal-arc welds in Table 4 and the elongations appreciably higher.

Much more test data are needed to define optimum welding procedures and to establish advantages and limitations of the process. However, the experience to date suggests that resistance to weld and fusion line cracking is quite good using nickel-iron filler wire. The process holds promise for applications where the production requirements merit its consideration.

Inert-Gas-Shielded Tungsten-Arc Welding: Nickel-Iron Filler

The response of Ductile Iron to inert-tungsten arc welding has been examined briefly employing nickel-iron filler, using the welding conditions given in Table 8.

Table 8

Tungsten electrode	$\frac{3}{16}$-in. diam
Shielding gas	Argon
Gas flow	20 cfh
Welding current	220 amp, d c
Polarity	Straight
Filler wire	$\frac{3}{16}$-in. diam
Welded plate	6 x 6 x $\frac{1}{2}$ in.
No. of passes	2 weave, 2 lapping

Sufficient work has been done to indicate that Ductile Iron of suitable composition for good weldability will respond quite well to this process. Present information suggests that the tensile properties of tungsten arc welds made with nickel-iron filler are somewhat similar to those of the metal arc welds made with nickel-iron electrodes. Welded joints on $\frac{1}{2}$-in. thick as-cast plate containing 3.5 TC, 2.5 Si, 0.35 Mn, 0.70 Ni and <0.05 P yielded about 67,000 psi tensile strength with 1% elongation in the as-welded condition, and about 61,000 psi tensile strength with 8% elongation after a postweld annealing heat treatment. Welded joints on annealed plate, tested in the as-welded condition, gave about 62,000 psi tensile strength with 7% elongation.

The observations on the use of the two inert arc processes are based on exploratory studies and much systematic information is obviously needed to obtain a comprehensive view of the possibilities as applied to Ductile Iron.

More needs to be known as to limitations of plate composition, conditions under which preheat should or must be used, optimum conditions of welding current, shielding gas and mechanical properties of welded joints, etc. However, the experience to date does indicate quite clearly that little difficulty should be encountered in obtaining welded joints quite resistant to weld and fusion zone cracking using both types of inert arc welding, with 60/40 nickel-iron filler wire.

It is probable that economic considerations will dictate the use of less expensive joining processes in most instances. Of the two phases of the inert arc process, the consumable electrode type is the more interesting, and may merit consideration for some types of fabrication welding, where production conditions warrant its use.

SUBMERGED ARC WELDING OF DUCTILE IRON TO STEEL

A brief study was made of this process in conjunction with an application calling for the automatic welding of Ductile Iron to steel, with the submerged arc method specified.

For laboratory studies, the test weld employed was a T-weld comprising a 12 in. long by 6 in. wide segment cut from some $\frac{3}{8}$-in. wall Ductile Iron pipe for the base and a vertical leg of mild steel, $\frac{1}{4}$ in. thick by about $2\frac{1}{2}$ to 3 in. high. This joint simulated the type of weld and the section thickness involved in the actual fabrication, though stress conditions were, of course, less severe.

A variety of nickel-iron or nickel-chromium-iron alloys were examined, most of which were inacceptable either because of excessive weld porosity or pronounced weld or fusion zone cracking tendencies, or both.

Of the various filler wires tried, the one which gave the best results with respect to weld quality was a Type 310 stainless steel wire of commercial origin containing 27.7 Cr, 21 Ni and 0.34 Si. The Ductile Iron casting contained 3.6 TC, 2.61 Si, 0.30 Mn, 0.38 Ni, 0.096 P and 0.048 Mg. Of the two proprietary melts examined, one designated "N" melt was found to be satisfactory.

The welding conditions found to give a good joint are listed in Table 9.

Table 9

Ductile Iron of low-weld hardenability, 0.10 P max
Use annealed castings
Filler wire, 25/20 stainless, $\frac{5}{32}$ diam
Flux, N-melt (proprietary flux)
Current, 400 amp
Voltage across arc, 40 v
Polarity, straight
Speed of welding, 20 ipm
Preheat, RT to 400° F
Postheat, 850° F—1 hr—SC, applied immediately, while weldment is still warm
Arc directed more toward ductile iron member.

The immediate postheat treatment is considered important to minimize hazards of fusion line cracking. Preheat did not seem important in the size of joint made for laboratory studies, but a moderate preheat of about 400° F would probably be useful in fabrication, where welds would be subjected to higher stresses during welding.

Figure 15 shows a typical joint, with cross sections bent with face and root in tension. The fact that mild steel was

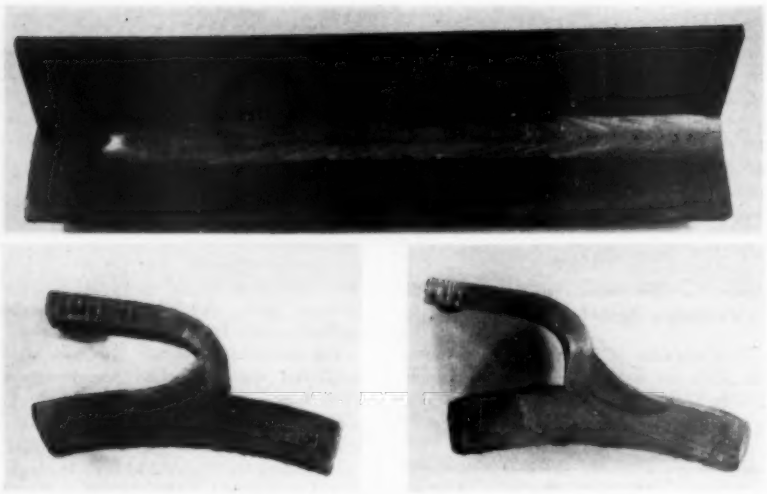


Fig. 15 Photos showing submerged arc weld of Ductile Iron to steel using 25–20 Cr-Ni filler wire

one member of the weldment undoubtedly lessened greatly the problem of avoiding fusion line cracking.

Preliminary welding tests by the fabricator interested in this job, made in accordance with the welding procedure of Table 9, were reported to be satisfactory, but, as far as is known, no commercial scale operations have yet been attempted.

While this process, as applied to Ductile Iron welding has obvious limitations, it offers some possibilities under proper conditions. Each potential application should, of course, be explored by trial welding tests, simulating the type of joint involved in the actual job.

SUMMARY

An attempt has been made to review the response of Ductile Iron to a number of arc-welding methods, including the metal arc, the inert-gas-shielded arc and the submerged-arc welding processes. Although not included in the scope of the present discussion which relates to arc welding, reference should be made to the oxy-acetylene welding process. This method, employing cast ductile iron filler rod, is being used successfully by several foundries to weld ductile iron castings.

On the basis of the present study, a few broad generalizations seem justified:

1. Ductile Iron can be welded by most of the commonly employed fusion welding processes.

2. Ductile Iron should be welded in the fully annealed condition for maximum welding response although it can be welded successfully in the as-cast condition with suitable selection of casting composition and joining process and procedure.

3. The lower the levels of those alloying elements in the casting composition which increase weld hardenability, the better is the welding response and the less important is the need for preheat.

4. Phosphorus has an adverse effect on weldability. A 0.10% maximum is permissible when welding with nickel-iron electrodes or filler wire. A 0.05% maximum should be observed if mild steel electrodes are employed.

5. The following nominal composition responded well to welding using several processes: 3.60 TC, 2.40 Si, 0.30 Mn, 0.70 Ni, <0.05 P, 0.05–0.08 Mg.

6. Metal arc welding with austenitic nickel-iron electrodes has been applied successfully to a fairly broad range of casting compositions in both the as-cast and annealed conditions, with and without preheat.

7. Inert arc welding with austenitic nickel-iron filler wire offers promise. Joints in Ductile Iron cast plate of preferred composition, welded in both the as-cast and annealed conditions were readily made in plate thicknesses up to 1 in.

8. Ductile Iron may be joined to such dissimilar metals as mild steel, stainless steel, nickel, Monel and Inconel with austenitic nickel-iron electrodes.

9. One of the above joint combinations, Ductile Iron-to-mild steel, is frequently encountered in field applications. The joint is readily made and, in general, offers fewer hazards with respect to fusion line cracking than those in which ductile iron comprises both members of the weldment.

10. Where machinability is a prime requisite, arc-welded joints should be fully annealed after welding (1650 + 1275° F treatment).

11. In the as-welded condition, joints on annealed plate are less troublesome to machine than those on as-cast plate.

12. Considerable improvement in the machining response of as-welded joints in castings of preferred composition for welding can be accomplished by special welding techniques and by brief torch annealing treatments, though machinability will be inferior to that of fully annealed joints.

13. A 1275° F postweld heat treatment (without the 1650° F prior treatment) may provide an adequate improvement in machining response for some purposes.

14. Tungsten carbide tipped tools offer substantial advantages over tool steel bits where a hard heat-affected zone exists.

15. Low hydrogen-type mild steel electrodes hold some promise for joining Ductile Iron, provided a low-alloy, low-phosphorus casting composition is used and the castings are welded in the fully annealed condition.

16. Submerged arc studies were confined to joining of Ductile Iron to mild steel. The process may be useful for special applications but its field appears limited on the basis of present knowledge.

A great deal of systematic study is needed in all the above fields to evaluate the advantages and limitations of the various arc-welding processes, to establish preferred casting compositions for maximum welding response and to develop improved welding procedures.

However, it is hoped that these observations and experiences will serve to demonstrate that Ductile Iron can be welded quite readily by several methods and will encourage further studies on the welding of this new engineering material.

ACKNOWLEDGMENTS

The authors wish to express their thanks to N. B. Pilling, Director of the Inco Bayonne Research Laboratory, and to Dr. W. A. Mudge, Manager of the Technical Service Section of the Inco Development and Research Division, for permission to publish this paper. A sincere word of appreciation is also due L. C. Minard and C. E. Witherell for their contributions in the preparation of welded joints and other assistance.

Advanced Automatic Flame Cutting for Machinery Weldments

Development and utilization of chain flame cutting in the fabrication of weldments for power shovels

by Howard B. Cary

INTRODUCTION

WITHIN the last ten years the major structural components of Marion Power Shovels have been converted from castings and riveted structures to weldments. This change in design created many changes in our manufacturing processes. Primarily, our steel foundry was abolished and our structural fabricating shop and welding shop were revolutionized. The Structural Shop was converted from one processing a large tonnage of hot-rolled shapes over saws, shears, punches, etc., to one processing an almost double amount of irregularly flame-cut, hot-rolled steel plate parts. Our structural department, which includes flame cutting, employs about 80 men and processes approximately 1200 tons of steel per month. A transition was made gradually in the flame-cutting section from semiautomatic burning machines following hand layouts, to template layouts and then to fully automatic burning machines, using aluminum strip templates. More recently, we have changed to large automatic machines using electronic eye tracer and finally to electronic eye chain burning. This paper will be devoted to the development and utilization of chain flame cutting in our plant.

PREVIOUS FLAME-CUTTING PRACTICE

All irregularly shaped plate items for our early weldments were cut with semiautomatic burning machines working to handmade layouts on the steel plate. Figure 1 shows the semiautomatic flame-cutting equipment in use. At a later date, layout templates were introduced and employed for repetitive parts. Their use reduced hand layout time, increased accuracy, eliminated much human error and provided more efficient utilization of steel plate stock. We still use semiautomatic burning machines for cutting large, nonrepetitive plate parts and most parts made from structural shapes.

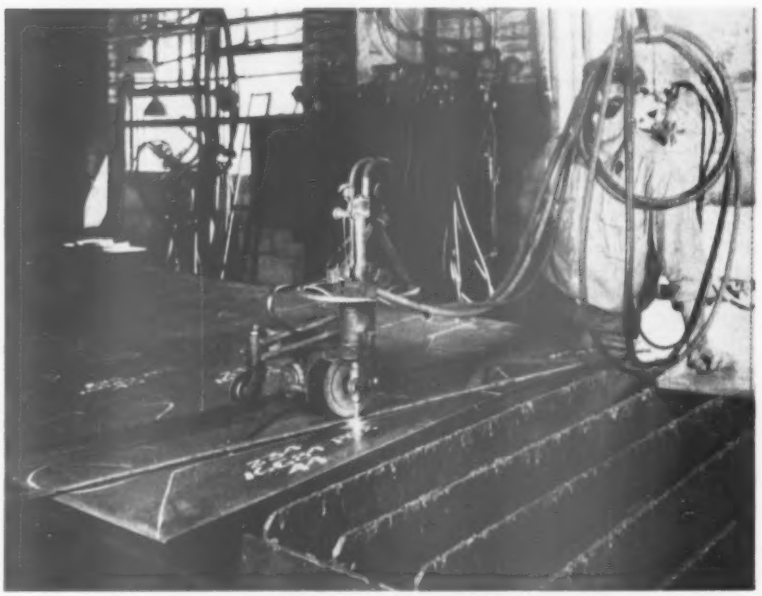


Fig. 1 Semiautomatic flame cutting to layout lines

Our company was among the first to use the double-carriage type automatic shape-cutting machines that were introduced in 1929. These machines were equipped with tracing devices using strip-type templates. The templates were made of small aluminum strips formed to the proper shape and fastened to a sheet metal backing. At first these templates were made only for fairly complex shapes or only for parts that were to be highly repetitive. Templates of this type had only one flame-cut part per template. As the number of weldments going into our shovels increased, we produced more parts on the automatic shape-cutting machines and finally added more double-carriage type machines. Also, the templates became more complex and larger until we exceeded the cutting range of our largest machine. This was temporarily overcome by using templates that had to be shifted in position to complete the cuts. An analysis of our cutting procedures revealed the following:

1. Automatic cutting was restricted because of inadequate cutting area of our machines.
2. Metal strip templates were expensive to produce.
3. Metal strip templates could not be constructed to produce certain desired parts.
4. Metal strip templates were difficult to alter, difficult to store and easily damaged.
5. Our machine could not use multiple torches except on small parts.

These limitations plus the need to immediately increase flame-cutting production capacity and the desire to reduce

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the cost of flame-cut parts led to a thorough investigation of the new electric-eye tracing equipment. This investigation, in turn, led to the eventual purchase of large-range, automatic flame-cutting machines equipped with electronic eye tracers.

ELECTRONIC EYE TRACING AND CUTTING

The Electronic Tracing Device has been best described by R. O. Fish in the December 1950 issue of THIS JOURNAL. It is necessary only to mention in this paper that by means of optical, electronic and mechanical hookups, the device will automatically follow the intersection of a light colored and dark colored surface. The template needs only to provide a white outline on a dark background or vice-versa. The automatic flame-cutting machine provides for one or more torches that will follow the identical pattern as the tracing device.

Our first machine with the electronic tracer was a pantograph type mounted on a stationary base (Oxygraph) that could cut a 52-in. circle or a 41-in. square maximum. It was equipped with four torches. Multiple torch operations cutting a multitude of identically shaped parts like those usually pictured in the advertisements did not fit our production schedule. This type of cutting on our product, due to limited production of like models, could not keep the machine busy. Continuous or chain type burning seemed more logical for our operation.

In Helmkamp and Yoch's paper in the March 1949 issue of THE WELDING JOURNAL, they mentioned the use of the electronic tracer to do continuous production cutting of like or dissimilar pieces by making a continuous template with bridging members to carry the cut along yet allow the pieces to be almost completely severed. In W. O. Springer's paper in the February 1950 issue, reference was made to the use of internested templates to use up scrap. These two references gave us the idea of combining or nesting on one template all parts of a particular plate thickness going into a given weldment.

EXPERIMENTAL CHAIN BURNING

The first test was on a small bracket used on one of our smaller machines. This shipper shaft bracket was designed entirely from $^3/_4$-in. thick plate except for two heavy rough machined bosses. There are only 13 plate items and the total weight of the completed weldment is about 200 lb. Figure 2 shows two of these completed brackets. Our first machine equipped with the electronic eye is shown cutting plates for two of these weldments in Fig. 3. The template employed for these parts is better shown in Fig. 4. The base or background material is black-tempered presdwood (Masonite) and the material for the outline is white pressure-sensitive (Scotch) tape. This template was our first attempt at combining on the one composite or multiple item template all parts going into a weldment. The large triangular shaped parts appear on the template four times even though they are identical in shape and the two different smaller triangular parts each appear four times even though they are identical, while the base plate appears only once. The white-colored tie bars or links connecting each piece together allow chain burning.

In cutting these pieces, we first pierced the plate with the tracer located at the small circle shown inside the cutout in the base plate. The tracer then would be manually led into the first small triangular piece to the left of the piercing point. The eye would "take over" control of the machine as soon as the spot of light centered over the intersection of the black and white surfaces. It would automatically travel around the lower edge of the upper-right small triangular piece, up to the tie bar, down the next piece, up the slope along the bottom then across the tie to the third piece, along the bottom

Fig. 2 Two shipper shaft bracket weldments

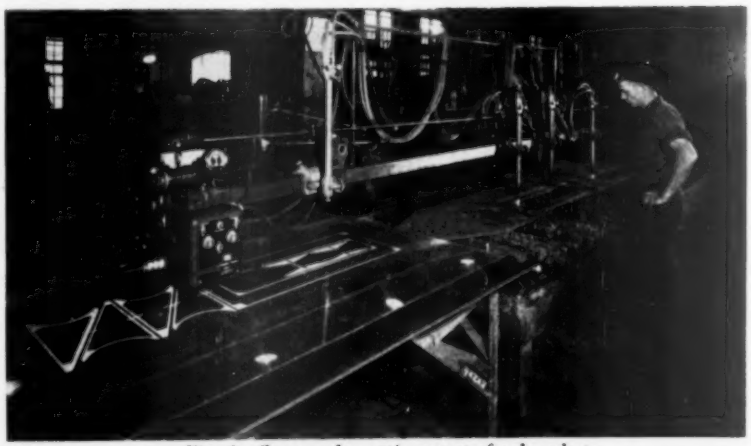


Fig. 3 Oxygraph cutting parts for brackets

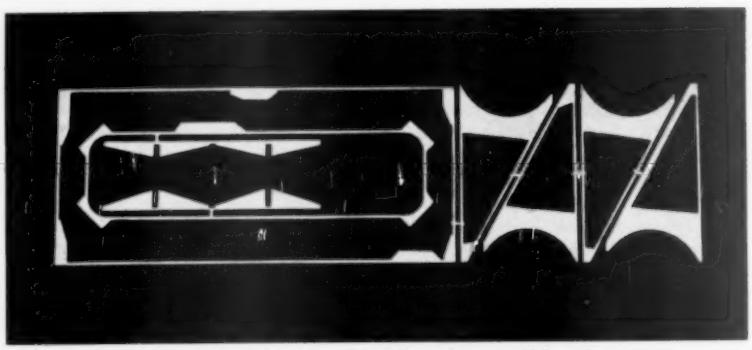


Fig. 4 Template for chain-burning small brackets

and then to the fourth piece. The eye would trace completely around it and across the link to the inner edge of the base plate. It would go across the top to the left, down and across the bottom to the right until it came to the tie strip. It would then go up and trace around the four lower triangular-shaped pieces, back across the tie strip to the inner cut on the tie piece, down and back to the right, up the right end and across the top to the left to the link, along the top of the three triangular-shaped pieces until it reached its starting point. The torch or torches followed this identical path and flame cut the steel plate. The cut would then be started on the outside edge of the base plate and it would continue until the base plate was cut around its outside edge and also around all of the large triangular-shaped pieces. Thus without stopping, we have cut out eight small triangular-shaped pieces and the inside of the base plate, except for the links holding all the pieces together. These two continuous cuts have thus produced all of the plate items necessary to make this weldment. If more than one torch is used, as was shown in Fig. 3, enough parts for several shipper shaft brackets are produced.

Cutting the tie members to separate the parts did not prove to be a problem. These were quickly burned with a hand torch and the desired parts separated from the resulting scrap steel. Bevels were called for on two edges of the large triangular parts. These bevels were cut on the blanks produced by the electronic shape-cutting machine in a secondary burning operation. Obviously beveled edges, required for full penetration weld joints, required more time to produce than square-cut edges. Thus, this first simple experiment with chain burning proved highly successful and opened the way to more complicated cutting.

The next weldment selected for chain burning was a small gear case and bracket weighing approximately 350 lb shown in Fig. 5. This case was originally designed to be made from $^1/_2$-, $^5/_8$-, $^3/_4$- and $^7/_8$-in. thick steel plate. During construction of the templates to produce plate parts for the weldment, it became apparent that it would be advantageous to keep the number of plate thicknesses to a minimum and thereby keep the number of templates to a minimum. The parts requiring $^5/_8$-in. plate were changed to $^3/_4$ in. thick and one template was eliminated. This, also, resulted in a hidden saving, since less plates needed to be handled to produce the parts for the weldment. There were several other parts in the weldment made from heavier material; however, since aluminum strip templates had been made and used before the acquisition of the electric eye equipment, it was decided to retain these templates and continue burning them on the double carriage machines. Only two $^7/_8$-in. thick parts were employed in the weldment; one was produced by sawing off a round bar and the other by a single-part electric eye template. It became apparent from making the templates for parts for this weldment that our design engineers and industrial engineers must thoroughly understand the chain-burning process, if we were to expect maximum utilization of its capabilities. Minor engineering design could eliminate the need for the $^7/_8$-in. thick parts and the parts from thicker plates could be changed to allow cutting from the same thickness plate and combined on one template.

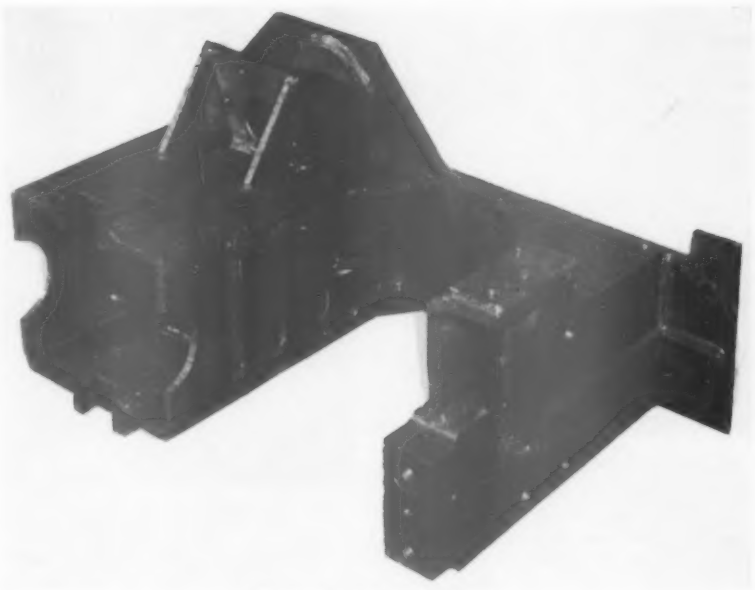


Fig. 5 Gear box weldment

The two multiple templates necessary to produce the steel plate parts for this weldment are shown in Fig. 6. The lower composite template is for the $^1/_2$-in. thick parts and requires a piece of steel 18-in. by 5 ft, 4 in. The upper template is for the $^3/_4$-in. thick parts and requires a plate 28 in. by 3 ft, $4^1/_2$ in. None of the plate parts going into this weldment required beveling; therefore, all flame cutting was completed after the tie bars were cut. Some of the parts required further processing in the Structural Shop, but this was the same whether the parts were chain cu.

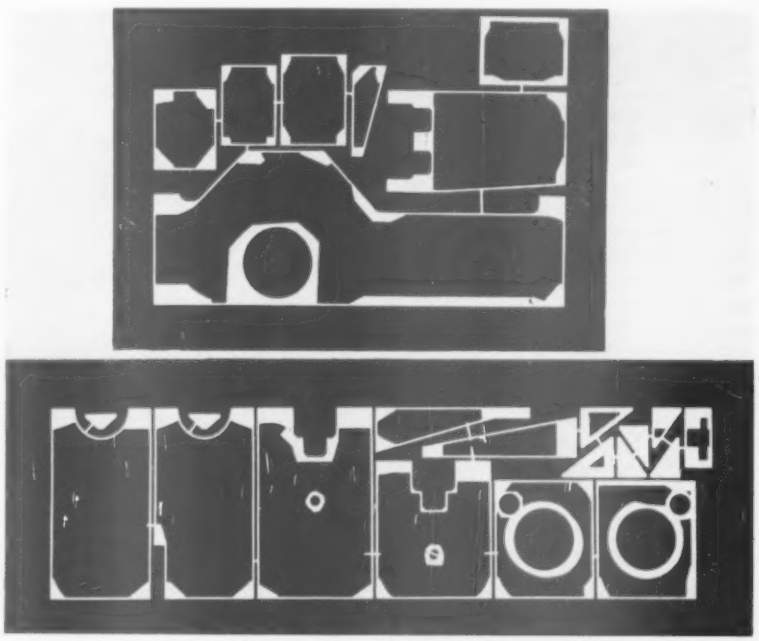


Fig. 6 Templates for chain-burning gear box

with the electronic tracer or cut using semiautomatic machines working from layout lines. The floor-to-floor time to chain burn was about half of the combined layout and semiautomatic cutting time. Of course, if two torches were used, the time per set of material was almost cut in half.

Another hidden cost savings was found. A complete circuit of the tracing head around all parts on the template was assured by the very nature of the chain-burning process. This complete circuit always produced the correct number of parts required for the weldment resulting in automatic production control.

TRAVOGRAPH INSTALLATION

The cost saving potential of chain burning of plate parts of the same thickness going into the same weldment became apparent after studying these two examples. It was decided to purchase two of the largest sized flame-cutting machines so that we could make larger templates. The large pantograph machine mounted on a carriage (Travograph) and equipped with the electronic tracer was selected. This machine is capable of cutting an area 144 in. wide and as long as necessary. A great deal of thought was devoted to engineering the installation to utilize the maximum capabilities of the machine combined with the desire of using large continuous chain-burning templates.

Our installation shown in Fig. 7 provided for two machines arranged so that each one could travel two-thirds the entire length of the common track. The cutting table is 146 in. wide and 86 ft long. It is arranged in small sections which can be easily lifted out to allow for slag removal. A heavy retaining wall built on the track side of the cutting table is very helpful for properly locating steel plates and for reducing possible damage to the equipment. The tracing table, for locating the templates, is approximately the same size as the cutting table. It is frame construction mounted on concrete piers. The gas and electrical supply is brought to the machines overhead by means of pulleys riding on stretched wires.

The 144-in. width capacity of the machine automatically limited two torch cutting to plate 72 in. wide, three torch cutting to plate 48 in. wide and four torch cutting to 36-in. wide plate. We, therefore, set up a primary requirement that large templates would be based on 72-in. wide plates. It soon developed that this could not apply to all plates, since many frame weldments of our shovels required wider plates, also many small weldments did not have sufficient pieces of proper contour to fill a 72-in. width without excessive waste. We, therefore, modified our requirement to allow 36-in. wide templates for the smaller weldments and templates up to 144 in. wide for the deck plates on large frame weldments. Template lengths were according to the material required.

Fig. 7 Travograph installation

We were able to standardize on 72-in. Universal Mill rolled plates 20 and 30 ft long for the majority of our requirements. This produced another hidden savings, since we had been purchasing many plates mill cut to odd dimensions, which added extra cost and limited their use to specific items.

Another problem developed when it was found that the black Masonite could not be obtained in pieces larger than 48 in. by 12 ft. In addition, it is necessary to allow 4 in. beyond the edge of a part to the edge of the Masonite. This is necessary, because the spot of light that traces around the edge of the template outline is separated from the driving wheel by almost 4 in. It developed that a template to cut a 72-in. wide plate needed to be 80 in. wide. This was obtained by using one piece 48 in. wide and one 32 in. wide. A special dowel plate was made to hold the various pieces of Masonite in proper alignment. The smaller sized pieces of Masonite were thus held together to obtain the required width and length of the large-sized multipiece templates. After completing the experimental chain burning and deciding on our basic principles, we began making large sized templates concurrently with the installation of the Travographs.

TEMPLATE CONSTRUCTION

Most of the template-making procedures were worked out during construction of the first few multiple-piece chain-burning templates. The template makers, who were all experienced layout men familiar with flame cutting techniques, tried out different procedures and proved them in production before they were adopted. We were able through this cut-and-try method to establish standard practices for making electric eye templates.

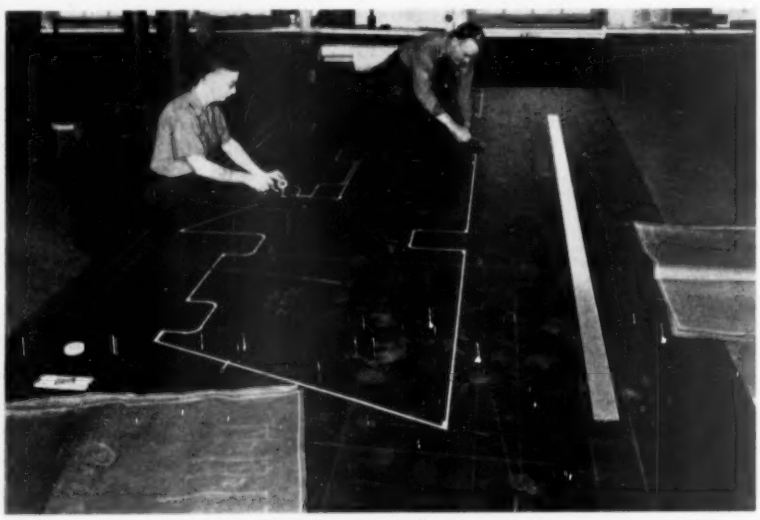
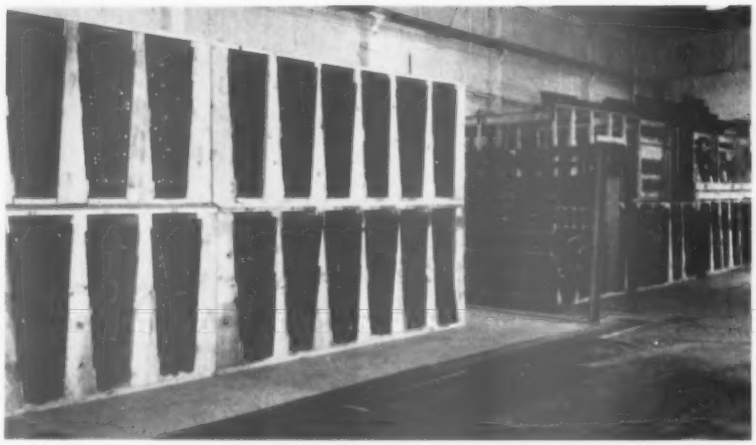


Fig. 8 Details of making templates

Fig. 9 Template storage racks



Our first templates for the eye were full-size cutouts of white paper layed on a black background. This provides a satisfactory template for single items. However, with paper cutouts, it is necessary to cover it with a sheet of transparent plastic material. We found that the plastic quickly became marred under the path of the tracer's driving wheel. The roughened area can be polished out, but it was a time-consuming operation. A harder plastic material that resisted the abrasive action proved to be too brittle for normal shop handling.

We made several single-item templates with white Scotch tape on black presdwood Masonite and found that they could be made in less time than the white paper cutouts. Figure 8 shows template makers at work on a Masonite template. We also found that it was not necessary to use the transparent plastic cover over this type of template provided that the Masonite backing material extended at least 4 in. beyond the edge of the template outline. We, therefore, standardized on this type of template construction for multiple-item chain-burning templates as well as for single-part templates.

Since a template would always be used for cutting parts from the same plate thickness, it seemed appropriate to show flame cutting procedure data on the template; therefore, cutting tip size, oxygen pressure, acetylene pressure and cutting speed appeared on each template as well as the template number, material size and specification and part numbers. All cutting on the electronic eye equipment is done with one type of tip; therefore, knowing the material thickness and tip size, the template maker determines the flame-cutting kerf width and makes allowances in his layouts. The kerf allowance is thus built into the template and if the specified cutting conditions are used the resulting steel parts are always identical. In addition to the above mentioned information, the chain-burning templates usually show the starting point, travel direction, bevel locations and template storage location. The first lot of material cut from a new template is inspected and if found OK, the template is approved by the Inspection Department and so marked.

Multiple-item-chain burning templates are not started until the template makers carefully study the complete weldment. At this stage, they may request design modifications or routing changes to allow more efficient templates. Elimination of plate thicknesses, change in part shape, standardization of steel specification throughout the weldment and change in

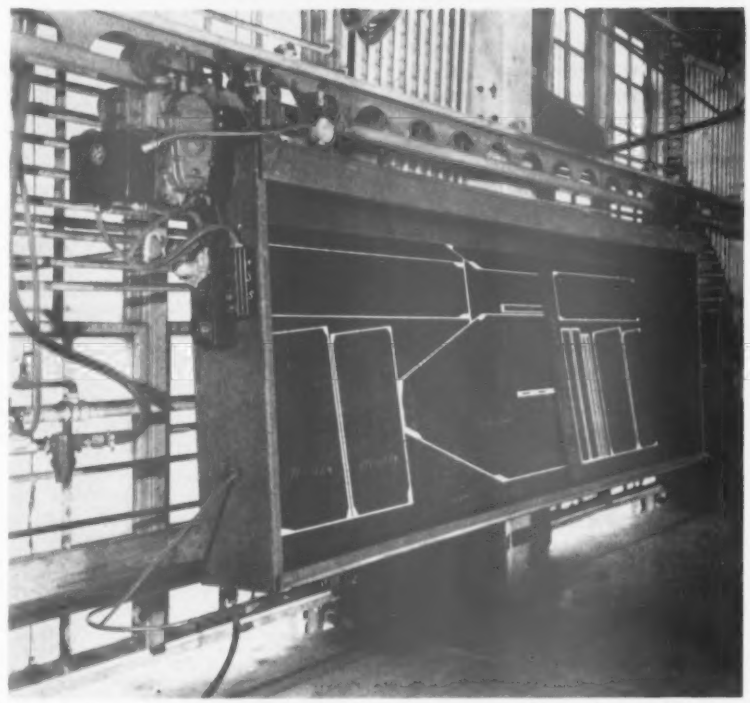


Fig. 10 Template carrier running between tracing table and template room

Fig. 11 Marion Type 372 1¾-cu-yd power shovel

routing to allow complete processing in the Structural Shop are only a few of the many items considered before starting the template. The least number of templates per weldment and the minimum amount of scrap per template is their objective. The template makers make every effort to efficiently nest the various parts in order to utilize all of the steel of the stock width plates. In order to minimize scrap, they sometimes work with scaled-down cutouts to find the best arrangement of parts. In chain burning, common cuts between pieces cannot be used. Also, we have found that it is not wise to use a plate edge for a side of a part. We have also standardized on showing parts in multiple templates the same number of times they are required in the weldment. In cases where the same identical part is shown many times, this may be an unwise decision, but for the vast majority of cases it is the most economical.

During our year and a half of making templates, we have made almost 2500. Figure 9 shows the storage racks for these templates. We have not found any deterioration of the templates during storage, except minor tape slippage when exposed to excessive heat. Template warpage has not been a problem. Efficient transportation between the template-making department and template table in the Structural Department is accomplished by a monorail carrier. This template carrier is shown in Fig. 10.

PRODUCTION CHAIN BURNING

Our first goal was to make electric eye templates for all weldments on our Type 372, a $1^3/_4$-yd power shovel shown in Fig. 11. We were engaged in a contract to produce these machines in quantity and it seemed logical to start with the type being produced in volume. This machine had weldments for the crawler frames, the lower or truck frame, the upper or rotating frame, the shovel boom and most of the other smaller components.

The lower frame weldment (bottom view) completely machined and ready for assembly is shown in Fig. 12. There are four different thicknesses of steel plate required on this frame, i.e., $^5/_8$, $^7/_8$, 1, $1^1/_2$ in. in addition to rough machined bosses and hubs. All of the steel-plate flame-cut pieces required to build six of these frames are shown in Fig. 13. The long straight pieces shown in the foreground of the picture do not require a template and are cut on a multiple-torch stripper, capable of making long straight, bevel or square cuts. The templates required for the top and bottom deck plates are shown on the tracing table during cutting operation in Fig. 14. Each of these templates is for a single part and does not involve chain burning. This picture, also shows the splicing of two pieces of Masonite necessary to get the desired template width. Both deck plates

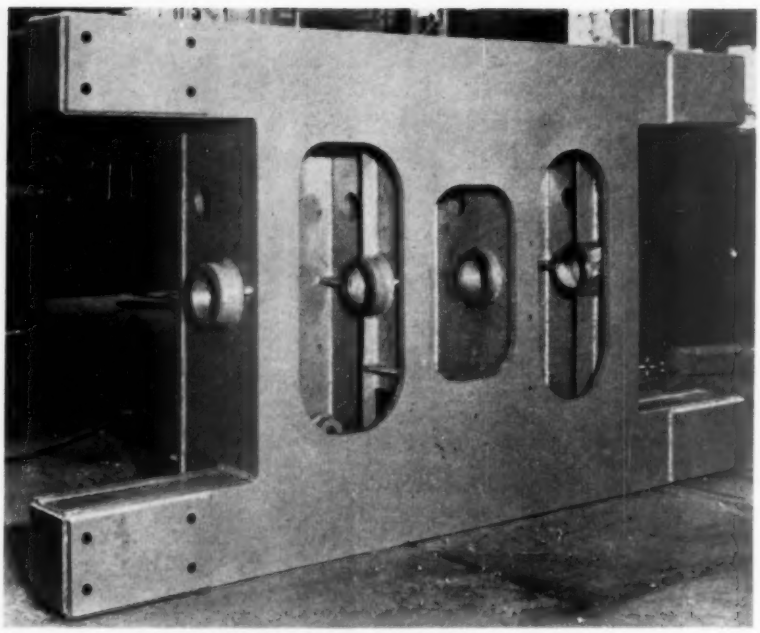


Fig. 12 Lower frame weldment (bottom view)

are $1^1/_2$ in. thick. When top or bottom deck plates are to be cut, two 72-in. by 30-ft plates are laid side by side on the burning table. The templates are traced and then moved along the table until the entire 30-ft of plate is cut. Additional 72-in. by 30-ft plates are used until the required number of decks are obtained for the particular production order. The entire length of the plates is not always used since the cutting requirements and the plate length may not be the same. In these cases, a smaller template may be used or the excess plate length sent to a salvage area for later use.

The templates for producing the parts from $^5/_8$, $^7/_8$- and 1-in. thick plate are shown in Fig. 15. The larger template illustrates the nesting of parts for maximum plate utilization. This template will cut 28 separate pieces from a plate 72 in. by 7 ft 5 in. In some cases, a part may appear four times, twice or only once, according to the number of times it is required in the weldment. In any case, a complete circuit of the tracing eye will produce all of the $^5/_8$-in. parts required for the lower frame weldment. The upper-right multiple template produces all of the $^7/_8$-in. plate parts; the upper-left template and the extreme left-hand template both, 1-in. material. Two templates are used here because in one case the parts weld together in a subassembly and are machined before going into the main weldment. This is one of the factors that pro-

Fig. 13 Plate parts required for lower frame weldment

Fig. 14 Cutting lower-frame deck plates

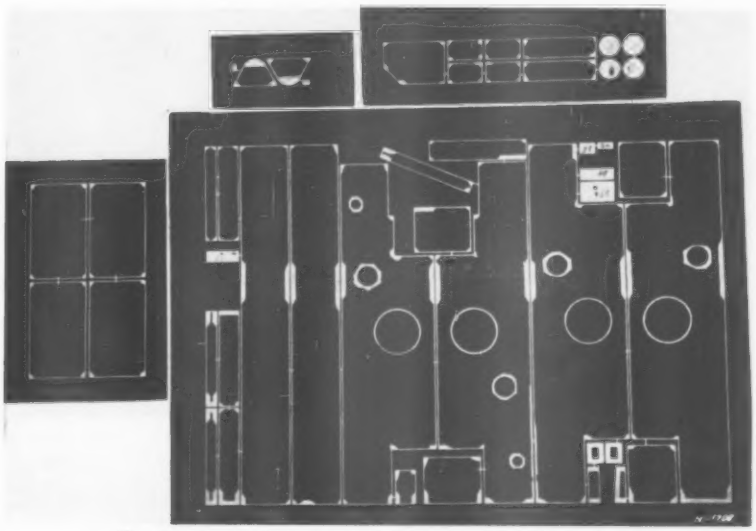


Fig. 15 Lower frame templates other than deck plates

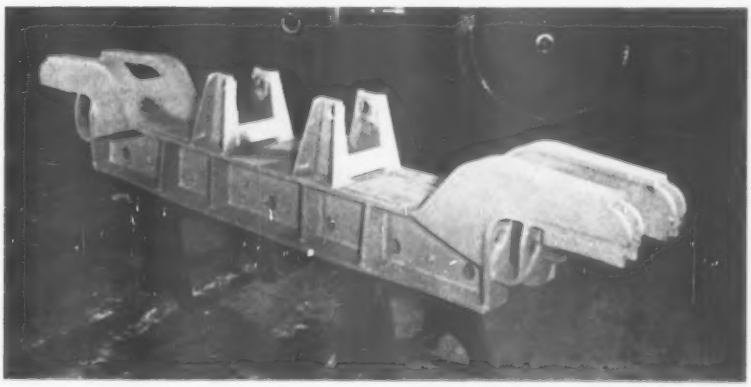


Fig. 16 Crawler frame weldment

hibits us from realizing the full benefits of chain burning. Our system requires that labor and material must be specifically charged to a part if the part requires processing in any department other than the Structural Department before it loses its identity in a weldment. This means that parts requiring a machining or forging operation or parts that are subassembled and machined before going into the major weldment cannot be on the same template with other parts of the same thickness. This increases the number of templates required and decreases efficient steel plate utilization. We hope to overcome this problem in the near future.

CRAWLER FRAME

A new crawler frame weldment, shown in Fig. 16, was designed specifically to take advantage of the capabilities of chain burning. It was designed entirely from two thicknesses of steel plate and, in addition, eliminated two end castings. The right and left crawler frame weldments were identical, which provided for greater volume of two torch cutting per production run. The weldment weighs about 3000 lb and is made of 26 pieces of 1-in. material and four pieces of $1^1/_4$-in. material. The two idler roller bracket subassemblies on the top of the frame are made of lighter material; however, the pieces are all sheared, welded together and the assembly machined before it is welded to the finished crawler frame.

Figure 17 shows the electronic templates necessary to make the plate steel parts for the crawler frame weldment. The major pieces of the frame are on the large template. This template cuts a 1- by 72-in. stock sheet 13 ft 3 in. long into 22 separate pieces. One complete circuit of the tracing head cuts eight identical end members, four each of two different sizes of stiffeners, two vertical longitudinal members, two bottom longitudinal members and two top members. The smaller of the separate templates is for $1^1/_4$-in. thick material while the other separate template is for 1-in. material. This smaller part is on a separate template because it is formed in the Forge Shop rather than in the Structural Shop. A number of the parts going into the weldment are hot-rolled bar stock sheared to length. This frame illustrates the potential economies available with chain burning if the designer fully understands the process. This redesigned frame is heavier and stronger than its predecessor yet its cost is the same. Redesign for chain burning rather than design modifications usually produce weldments that more fully realize the potential economies.

UPPER FRAME

The upper or rotating frame of this machine, shown in Fig. 18 (bottom view), had

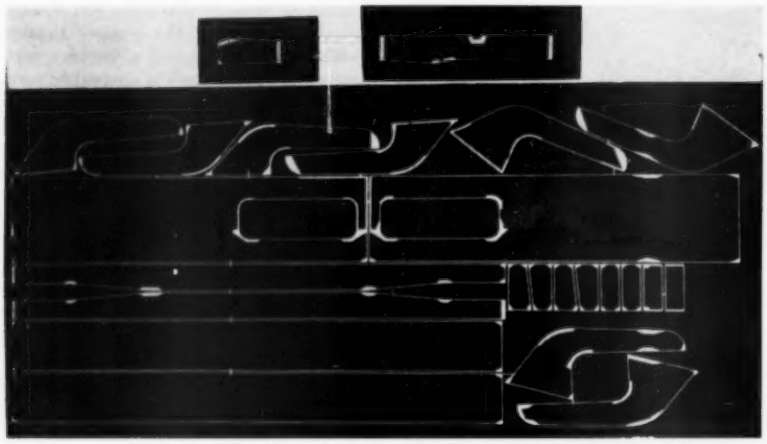


Fig. 17 Templates for chain-burning crawler frame

been a weldment for many years. The early design featured weight economy; however, modifications through the years introduced extra material thicknesses. The frame was completely reviewed to adapt it to chain burning and the variety of plate thicknesses was reduced from 12 to 5. This provided additional cost savings, by reducing the number of plate thicknesses procured and stocked.

The upper frame weldment had by far the most pieces of any component in the shovel. There are 170 pieces cut from $^1/_4$-, $^1/_2$, $^3/_4$-, and 1- and $1^1/_2$-in. thick plate. Parts cut from $^1/_2$-in. plate are the most numerous; so many, in fact, that they completely fill one 72-in. by 30-ft template and overflow to a 18-in. by 2-ft $2^1/_2$-in. template. Figure 19 shows double torch burning the $^1/_2$-in. plate from the template that carries 72 pieces. The machine has been traveling from left to right. It requires over eight hours of continuous burning to cut all these pieces. The parts are marked for identification before the tie members are cut. After cutting the links, the scrap is cut into small pieces to allow handling and is removed. The production parts are then gathered up and removed to another area for slagging and any subsequent operations in the Structural Shop.

We have experienced some difficulty with extremely small pieces nested among the larger ones. On this particular job, we leave some of the smaller pieces tied together until they are removed from the burning table. This has helped eliminate losing some of the smaller pieces.

This frame is finally built in the Welding Shop from many subassemblies, some of which have machining operations prior to welding into the frame. On this weldment, we did not follow the procedure outlined for the lower frame, since the templates ignored all subassemblies and put all parts of the same plate thickness on the same template without regard to assembly procedures. This allows much more efficient burning on the electronic eye equipment. However, it places added responsibility on the Structural and Welding Shops, since they must keep the time separate for these subassemblies and must sort out parts going into them, weld them up, have them machined and ready for the final frame weldment assembly.

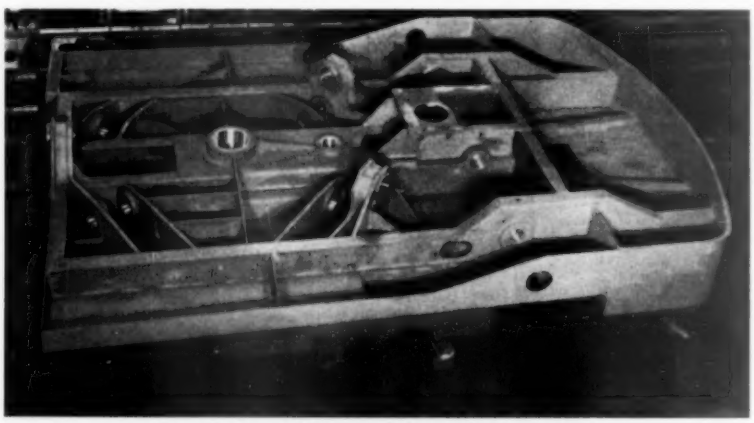


Fig. 18 Upper frame weldment (bottom view)

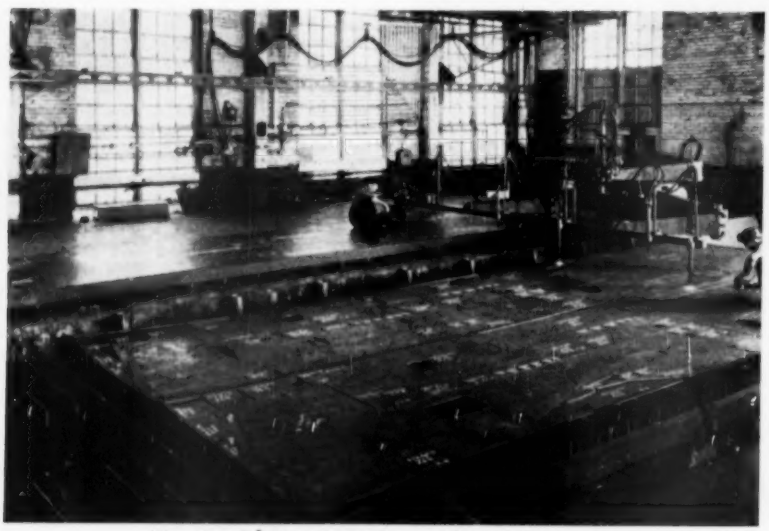


Fig. 19 Cutting upper frame 1/2-in. plate parts

PRODUCTION CHAIN-BURNING PROBLEMS

Our experience with chain burning disclosed many problems unforeseen when we started. Engineering changes on a few parts of an established multiple-type chain-burning template can be troublesome. When the geometry of a part is changed in any manner that does not involve interference with adjoining nested parts, it presents no problem; however, if a part's size is increased or the geometry changed so that interference occurs between adjoining pieces, it requires shifting several parts. Engineering changes that require the part to be made of a different thickness, of course, mean that the part must be removed from one template and added to another. This type of change, fortunately, has been held to a minimum.

Warpage during burning caused many problems. We found that some of the

parts would go out of square while burning a multiple-type template. Investigation disclosed that the uneven shrinkage stresses due to heating and cooling caused excessive plastic flow in the tie bars holding the various parts together. This situation was aggravated, of course, when the second side of a tie bar was cut and the entire link became red hot. This problem was solved in many cases by merely doubling the width of the tie member. In a few cases, it was necessary to use two tie bars. It has also been necessary to rearrange the location of tie bars and reroute the tracing path. Warpage of long slender pieces cannot be entirely overcome. Whenever the length to width ratio becomes too great, it is not practical to cut it on one side and then the other without getting a small amount of camber.

Another problem, briefly mentioned above, deals with nesting large and small pieces together indiscriminately on a large template. In a great many cases, the smaller pieces tend to become mixed with the cutouts and scrap and become lost. On later templates, we have attempted to keep smaller pieces together and somewhat segregated from the larger ones; however, this cannot be carried too far without sacrificing efficient plate utilization.

Many of the parts in our weldments have round cutouts where a boss or hub is welded in and, in most cases, these joints require a bevel-type weld joint. We originally cut out the opening with square edges on the electronic burners and then beveled the edge with a hand torch. It developed that by leaving in a tie bar to locate the center of the cutout we could use the semiautomatic flame-cutting equipment equipped with a radius rod and bevel head and obtain a higher quality cut in less time. This procedure was adopted for all beveled round holes except the very small ones.

Tip deterioration during long continuous cuts is a problem. It is our practice to carefully check and inspect tips before starting a long chain cut, also to immediately stop the cut and clean the tips if any flame trouble develops. This is one of the reasons we employ two operators on our machines. Another is that waviness occurs in the thinner plates and in certain alloy plates during cutting. This requires constant torch adjustment for high quality cutting. Plate riders do not seem practical to maintain proper tip to plate spacing for chain contour cutting.

Preventative maintenance is a must on the tracing equipment and the Travograph in order to maintain maximum output. Continuous chain burning for long periods of time and on three shifts squeezes years of normal operation into a few months.

Cleanliness is probably the next most important item. Templates, template table and carriage rails must be kept clean for trouble-free operation.

CONCLUSION

We are very pleased with the results obtained with these complex flame-cutting templates for use with the electronic tracer. Furthermore, we believe that our capital expenditure for the equipment and the investment in templates has been justified. The savings made possible by our electronic chain-burning operation can be briefly summarized as follows:

1. Reduced cost of steel plate items for weldments due to:
   - (*a*) Elimination of most hand layouts.
   - (*b*) Automatic cutting is faster than manual or semiautomatic cutting.
   - (*c*) Double, triple and quadruple torch cutting can be employed.
   - (*d*) Complex parts can be cut as quickly as simple ones.
   - (*e*) Efficient, consistent utilization of steel plates producing maximum parts and minimum scrap.
2. Reduced cost of weldments due to:
   - (*a*) Better fit-up resulting from consistently accurate parts.
   - (*b*) Reduced cycle time due to elimination of one major operation i.e., layout.
3. Improved flame cutting flexibility:
   - (*a*) Enables us to automatically cut intricate shapes not possible previously.
   - (*b*) Templates are quick and easy to prepare.
   - (*c*) Templates are easy to alter or modify.
   - (*d*) Templates are lightweight and easy to store and maintain.
   - (*e*) Flame-cutting equipment has large cutting area.
4. Intangible cost savings:
   - (*a*) Standard size stock plates are used reducing raw plate inventories and mill cutting extras.
   - (*b*) Elimination of some plate thicknesses with accompanying procurement difficulties and inventory problems.
   - (*c*) Better material flow because templates provide a degree of production control.

In the very near future, we expect to have electronic templates for every weldment going into regular production of our standard line of machine. Before concluding, I would like to offer a challenge to manufacturers of flame-cutting equipment and pressed-wood board:

1. Increase the width of the cutting range to 180 instead of 144 in. This would enable twin torch operation on 90-in. plate which is the maximum width obtainable without paying width extras.

2. Provide automatic beveling on contour cuts keyed to the templates in such a manner that on edges requiring beveling the templates will be so marked and the torches will automatically cut the proper angle of bevel.

3. Make pressed-wood hard board in larger sheet sizes to reduce template splicing.

ACKNOWLEDGMENT

I would like to express my appreciation to C. W. Ziegler, Manufacturing Manager, and L. E. Gillett, Methods Engineer, for encouragement during preparation of this paper.

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Fusion Welding of Light-Gage Alloys

Design of suitable jigs and selection of proper welding process assure the successful welding of aircraft assemblies

by J. T. Maloney

IN THE design of aircraft aluminum-welded assemblies, Engineering is constantly trying to keep the weight of the finished product to a minimum and still keep the strength of the assembly to the requirements. Therefore, the alloy or alloys called out for the job require higher tensile and fatigue values than most of the weaker alloys, such as 2S or 3S. Because of the alloying constituents that are necessary to bring the alloy to required strength, Production and Tooling have problems such as cracks and strains that occur during welding. Here very careful planning is necessary to determine how the parts are to be jigged and which welding process, either oxy-hydrogen or inert-gas metal-arc welding, is to be used. Generally speaking, we try to use oxy-hydrogen welding on assemblies where distortion can be easily controlled. This process is used by us on butt, standing edge or melt down welds where no filler metal is used; inert arc welding is used on all lap, fillet and auto-machine welds.

We have found that the cost of oxy-hydrogen welding is considerably less than manual inert arc welding for the following reasons:

1. The cost of the gas is much cheaper.
2. The welding speed per minute is higher.
3. Flux can be used and the penetration or underbead of the weld is much smoother and free from oxides which are present when the under bead of the weld is not protected.
4. Most of the stampings used in the assemblies are drop hammer formed and are scribed to a template and trimmed to the scribe lines by saws or metal shears. Hence, the tooling cost is much cheaper than it would be if they had to be trimmed by a high-speed rotary cutter or machined, as is sometimes necessary in machine inert arc welding.
5. Because of the high amount of concentrated heat in inert arc welding, distortion is a major problem necessitating expensive holding fixtures, such as backing bars and hold-down fixtures which have to match the contour of the weldment to minimize distortion.
6. The initial cost of the equipment and the maintenance is much less than that of the manual inert arc process.

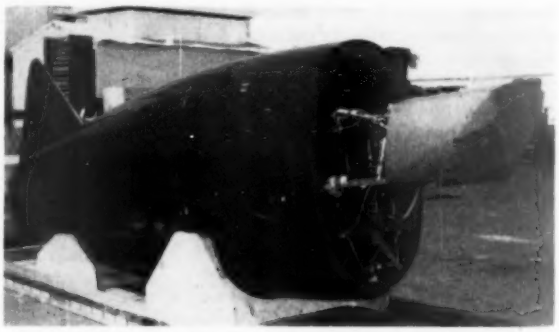

Fig. 1 Auxiliary wing-tip fuel tank

Figure 1 shows an Auxiliary Wing Tip Fuel Tank, which Rohr Aircraft is manufacturing, for one of Lockheed Aircraft's current model patrol bombers. This is a 350-gal. capacity tank and attaches to the wing tips by two balls and sockets and a hook, which makes it an expendable tank. The nose is later covered by a plastic dome and contains radar or searchlight equipment. The tank consists of four major assemblies which are the top shell, bottom shell, tail assembly and well or tunnel assembly to which the tank is attached to the wing tip. It is almost entirely a welded assembly.

The main body of the tank is made of 61SW and is assembled in this manner: Reinforcing bulkheads are resistance welded to the two half shells. The shells are given the initial forming on a stretch press in the S. O. condition. They are solution heat treated and while still in the as-quenched condition, they are finish formed again in the stretch press, after which they are rough trimmed and have tooling holes drilled in them that are used for future indexing. The shells are located on the spot-weld jig which picks up the inside contour of the skin and the tooling holes. The bulkheads are then resistance welded to the various station lines of the shell. The next step is to retrim the upper shell to match the well assembly, after which the two assemblies are joined in a weld jig, using the same

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tooling holes that were originally put in. The two half shells are then trimmed for the longitudinal weld which joins them together. This is done by locating to the inside contour and trimming by a high-speed rotary cutter. The two half shells are then joined by inert arc welding. After welding, the welds are all checked by the dye penetrant method in which a red dye with a very low surface tension is applied to all of the outside weld beads. A white duplicating fluid is later applied to the inside beads which give evidence of any leaks by the dye leaking through. After the tank is completely assembled, it is pressure tested to 5 psi per square inch and checked with soap suds. If there are no weld repairs, it is filled with a solvent which is left for 4 hr before again checking. This, we feel, is necessary to assure that we have taken every precaution against leakage before it is finally accepted for shipment.

Figure 2 is an illustration of one of the well or tunnel subassemblies. It consists of two drop-hammer stampings of 0.092 in., 61SW material. These parts are scribed to a template and are trimmed to the scribe lines by means of a band saw. The parts are then chemically cleaned by a solution of 0.3% nitric acid and 0.3% hydrofluoric acid in water to remove all surface oxide. The weld jig is made of solid Kirksite and matches the inside contour of the parts. This type of jig is very inexpensive as it can be reclaimed by remelting. Two full-length shims of 0.015-in. aluminum are inserted upright underneath the skins. The assembly is then tacked approximately 2 in. apart using 43S welding rod with oxy-hydrogen gas. The fitter works with the welder using pry bars to control the gap which is held to approximately $^3/_{32}$ in. This, we find, is an ideal gap on this thickness as it keeps distortion and cracking to a minimum. We believe that the lower melting point of the 43S rod alloys the strains to be taken up in the weld metal istelf which is more ductile than the parent metal adjacent to the weld. We have found that in welding 61S material with a 61S rod, the weld is more subject to cracking and distortion. After the parts are tacked, they are taken out of the jig and

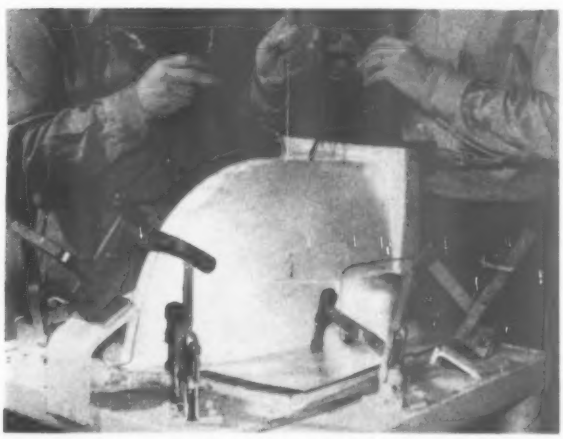

Fig. 2 Well or tunnel subassemblies

Fig. 3 Assembly weld jig of tunnel or well

fluxed on both sides using one of the popular brands of flux mixed with distilled water. Welding speed is approximately 6 ipm. This, of course, varies with different operators. After welding, assemblies are defluxed in a solution of 10% sulphuric acid in water. If repair is necessary, voids or pinholes are ground out, wire brushed and repair is made by the inert-gas-shielded metal arc process using the original rod.

Figure 3 illustrates the assembly weld jig of the tunnel or well assembly. Since this assembly is the backbone of the wing-tip tank, it is necessary to hold it to a very close tolerance to assure that it mates perfectly with the wing tip itself. There are contour blocks set up on the various station lines to which the subassemblies have to be clamped rigidly to make sure there is little or no movement during the 1400 in. of welding that has to be done on this jig. The two center sections are clamped in the jig, both from the inside and outside, and are butt welded both on the top and bottom, using the oxy-hydrogen process. Then the top and bottom subassemblies are also welded, using the same process. After this is done, the assembly is hand cleaned on the outside to remove all flux. Structural tees are then inserted into the bars that are slotted to receive them. These bars are clamped to the assembly and, like backing bars, protect the weld area from the inside. The welding is done by the inert-arc welding process using an a-c transformer and argon gas. The average speed is approximately 10 ipm. This, we feel, is due to the fact that there is little or no chance for distortion between the structural tees and stampings because both are held rigidly from top and bottom. The procedure and sequences used are ones that were picked up by trial and error but do give us an assembly that meets all the requirements. Since the jig is very

Fig. 4 Joining of well assembly to half shell

heavy, it is mounted on a trunion, counterbalanced and equipped with an air brake so it can be securely locked in any position with 360 deg radius, thus providing easy access for both the metal fitter and welder. After they are welded, the structural tees, which are also of 0.092 in. thickness, 61SW material, have four jig-located tooling holes drilled in them for indexing in succeeding jigs.

Figure 4 illustrates the joining of the well assembly to the half shell. These assemblies have previously been trimmed by a high-speed rotary cutter which assures the proper gaps for welding. They are attached by means of tooling holes, drilled previously, assuring the proper relationship between each other. The shell is then clamped to keep down movement during welding. As the half shell is 0.072 in. thick compared to 0.092 in. of the well assembly, more distortion problems are encountered in the half shell. This is controlled by keeping the tacks to a minimum and welding, as much as possible, free hand. In this operation, the skill of the metal fitter is of great importance. It is up to him, by the use of pry bars, to keep the material spread out far enough so that there is no chance of overlap occurring due to shrinkage. Ordinarily, we do not recommend inert arc welding for this type of work but, in this case, there is no alternative because of the fact that some of the stiffeners which are resistance welded to the shell in close proximity to the weld area are of 24ST. The amount of preheating required for oxy-hydrogen welding would cause a loss of corrosion resistance and possible loss of strength in the 24S material.

Figure 5 shows the jig used in making the two longitudinal welds which join the two half shells together. Due to the fact that weld line is only $1^1/_2$ in. from the well assembly, it took a great deal of skill and thought on the part of the Welding Engineer and Designer to build a set of backing bars that would accomplish their purposes and be removable from the tank after the welding was complete. It was finally decided that the bars could be no wider than 2 in. because they also had to clear the bulkheads in the upper half of the tank. This meant the bars would have to be collapsible. This was accomplished by means of a screw-type eccentric which allowed the jig to collapse enough to clear the upper bulkheads for removal. After the two shells are trimmed longitudinally. They are hand cleaned to remove any oil and wire brushed to remove any surface oxide. Then, they are clamped to the backing bar by clamps that pick up tooling holes in the bulkheads. There are two Cal-Rod units running the full length of the bars which are thermostatically controlled. These are set at 110° F, not for the preheat value, but to maintain an even temperature which insures even penetration. For the purpose of alignment and constant root opening, shims are inserted into the backing bar which has a $^1/_{16}$-in. slot running the full length of the bar. There is also a $^3/_8$-in. V-shaped groove running the whole length to allow for penetration.

After the shells are assembled, they are inspected for gap and alignment and rolled into position. Figure 6 shows the shells raised into position for welding. This is done by two hydraulic jacks—one located at each end of the jig. The upper part of the jig is recessed to receive the bottom so that when it is raised into position, there is perfect alignment between upper and lower fixture. After the skins are clamped in place from the top, the shims are removed from the backing bar in preparation for welding. The welding is accomplished by the mechanized inert-arc process using a nonconsumable tungsten electrode with a automatic voltage

Fig. 5 Jig for longitudinal welds which join two half shells

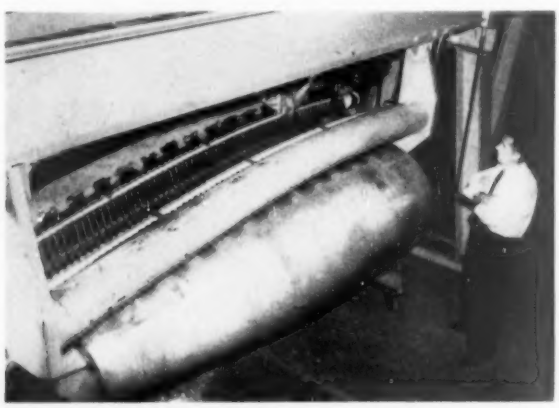

Fig. 6 Assembled shells raised in position for welding

Fig. 7 Clamping arrangement for holding shells against backing bar

control to regulate the arc gap. The current is generated by an a-c transformer. The welding carriage is powered by a $^3/_4$-hp variable speed motor which runs parallel to the weld above the tank. Due to the teardrop design of the tank, it was necessary to make a floating type head set on wheels which run on a cam built to the general contour of the tank. The wire is $^3/_{64}$ in. in diameter, of 438 material and is run through a rod feed that has a series of straightening wheels. The rod is fed from 40 to 50 ipm and the welding speed is 8 ipm. The speed was set at this because we felt that, at this speed, the operator would have complete control in case of any trouble. At a higher speed, he would not be able to shut off the machine fast enough. The shells are 0.072 in. thick in one area and 0.092 in. thick in the area of the well assembly. The welding is started at the area which is 0.072 in. thick and, therefore, it was necessary to get a quick increase in heat to offset the heavier thickness. This was accomplished through a series of resistors which cut the amperage down 15 amp and then by-passes these resistors when

Fig. 8 Production line of wing-tip fuel tanks

the heavier materials are reached. A switch at the welding head makes this operation instantaneous.

The clamping arrangement is illustrated in Fig. 7. There are two banks of 70 individual L-shaped clamps that hold the shells securely against the backing bar. They are actuated by inflating the fire hoses and the banks are individually operated with a spring-type retrieve. As the weld is 8 ft long, the edges are gapped $^3/_{16}$ in. at one end. The individual pressure on each clamp is 60 lb, which is just enough to hold the edges down yet allow the heat to pull the edges together. There are two 6- x 8-in. welding lenses mounted on the front and side of the head which allow the operator to observe the welding from any angle as the welding progresses. The amperage meter, heat and rod feed adjustment are conveniently mounted on the front of the head within easy reach of the operator.

This production line of wing-tip tanks, shown in Fig. 8, is in itself a summary in that both oxy-hydrogen and inert are welding have a place in the welding of alluminum alloys. Each has its merits and also its disadvantages. But, combine the two and you have all the tools to fabricate a quality assembly out of any weldable aluminum alloy. There tanks were our first experiment in the larger assemblies where a rejection would have meant the loss of a considerable amount of material and labor. To date, some 400 of them have been manufactured including the proto type which passed all tests as to performance and safety. Therefore, it is possible to fabricate any weldment by taking advantage of each welding method and, with proper tooling, design and production "know how," be able to say, on completion, that the job is well done.

PLAN TO ATTEND

AWS 34th NATIONAL FALL MEETING

WEEK OF OCTOBER 18, 1953

HOTEL CLEVELAND, CLEVELAND, OHIO

Some Considerations on Weldability of Aluminum Alloys

A review of the available international literature and a discussion of some of the problems associated with the weldability of aluminum alloys

by J. Koziarski

I. INTRODUCTION

A CANDID review of the available metal literature would quickly show that, in general, the light alloys seem to have been somewhat neglected by metallurgical writers on this side of the Atlantic Ocean. Much has been published about ferrous alloys but very little about aluminum and magnesium.

While it is true that in 1951 the annual production of aluminum in the USA was less than 8% of the total production of steel in this country, the total production of aluminum in America (without Canada) was quite impressive (836,881 short tons[1]) as compared with the total Western European (on the free side of the "Iron Curtain") production (368,950 short tons[1]).

In spite of this showing, the number of American research papers on light metals (particularly on the welding of aluminum) is only a small fraction of those published in this field in Great Britain, France, Germany and Switzerland. This interest of the Old World in light alloys is probably due to the steel shortage which faces European countries, as demonstrated for instance by the interest shown by the British Admiralty in aluminum alloy welded ship hulls. It must be expected that Russia, too, is making similar progress in the field of light alloys.

If aluminum is to occupy the place it deserves in modern industry and everyday life, much more information is needed. Much fundamental research and development work must be done if light metal alloys, in general, and aluminum alloys, in particular, can achieve an equal stature with steel and cast iron in our industry. In fact, there should be no competition between aluminum and ferrous alloys, because they are not substitutes for each other. The physical and physical-chemical properties are so different that they should find rather parallel and not competitive applications.

It is true that aluminum and magnesium alloys do not enjoy the benefits of centuries-old tradition and know-how available in the use of iron. Consequently, first of all, this tradition must be replaced with the conviction that although they are new-comers, the light alloys are at least as good and sometimes even better than the old-timer, iron. It also seems that in the near future aluminum and magnesium alloys may face a true competitor, in the light alloys realm of aircraft. This is the new and very favored child of our modern technology: titanium.

The light metal industry must produce more aggressive activity in order to show the customer that its products may find more and more application. This industry should not wait for the demand. On the contrary, it should create one.

In recent years some progress in this country has been made. A few excellent research papers on weldability of aluminum alloys have been published. This, however, should be considered only as a good beginning. More fundamental data and more development work are still needed.

Aluminum would find much larger application in everyday life if the manufacturing cost from this metal can be reduced. This, first of all, could be achieved by improving the weldability of high-strength aluminum alloys. Although it is true that the pure or commercially pure aluminum possesses excellent weldability, this is not true of aluminum alloys.

This brings up a fundamental error which seems to prevail throughout the metal industry of the whole world. When a new alloy is created, it often becomes necessary to explore or improve its weldability. Would it not be better, in the case of an alloy intended for welding, to be conceived with this purpose instilled in it? It is also a fallacy to try to produce a universal weldable alloy, i.e., an alloy which would possess equally good weldability whether in thick or thin sections. This is because the speed of physicochemical reactions and transformations among other factors depends on time; and time of welding is longer for thick than for thin sections. This is equally true for light metal alloys as for other alloys as well.

It might be well at this point to mention that this is not a research paper but rather a review of the available international literature and a discussion of some of the problems of weldability of aluminum alloys which are of particular interest to the aircraft industry and with which the author has been concerned for over 20 years.

Figure 1 gives some terms related to an aluminum alloy welded joint. These terms will be used in this work.

Since the author has been chiefly concerned with thin-gage alloy stock, as used in aircraft design, the following discussion will deal with aircraft gages of material.

Notes: (*a*) The Appendix contains etching solutions as used for preparation of this work and chemical composition of alloys discussed.

(*b*) Etching solutions are indicated by capitals preceding the magnifications of photographs.

II. PROBLEMS IN WELDABILITY

By the term "weldability" one should understand not only the ability of a metal or alloy to produce an unseparable joint under heat but also to provide the required joint properties. Consequently, an alloy may be weldable for decorative purposes but may not be for highly stressed parts. The weldability of an alloy may be satisfactory for statically loaded domestic utensils but may be inadequate for dynamically loaded aircraft components.

Foundry research may be of great help when studying weldability problems. In fact, welding is a die-casting process on a miniature scale. Consequently, the student of weldability of aluminum alloy

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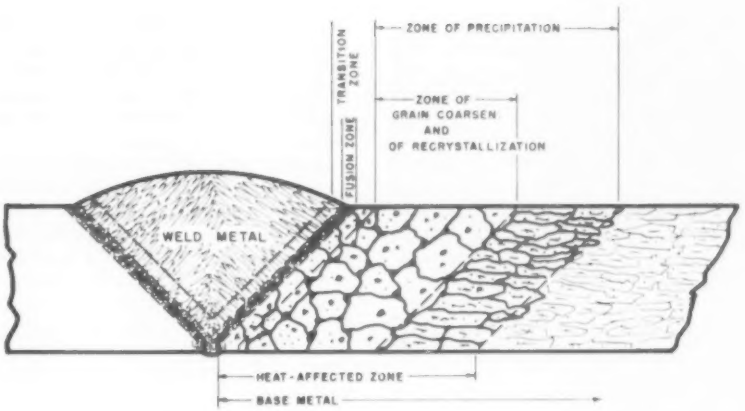

Fig. 1 Schematic cross section of an aluminum alloy weld

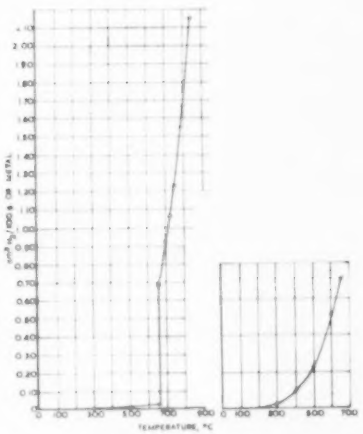

Fig. 2 The solubility of hydrogen in the pure aluminum at 1 atm hydrogen pressure[4]

should not overlook the experience already gained in aluminum casting. The literature in the latter field is relatively abundant.

Care should be taken, however, when interpreting casting results with the thought of applying them to welding. There exist large differences in mass, time and temperature involved. The mass in casting is generally expressed in pounds, if not in tons. The time of reactions or transformations is relatively long and the temperature is strictly limited, controlled and relatively uniform throughout the mass. In welding the opposite is true. The masses involved in reactions or transformations may consist of minute fractions of an ounce, the time may be a fraction of a second and temperatures may show hundreds of degrees difference in a distance of a fraction of an inch. Besides, temperature cannot be as well controlled in welding as in casting. In welding it may extend from the boiling point of the alloy to room temperature. Even the ring test, so much preferred by some research workers, cannot be directly interpreted for welding. This is because the ring solidifies as a whole and the weld freezes progressively. In summarizing, one may say that casting experience may be a help to better understanding of welding but the final answer may be expected only from the actual welding.

A student of weldability must always keep in mind that:

(*a*) The weld metal is cast metal and the base metal may be cast or wrought. The latter may also be rolled, forged, extruded, cold worked or heat treated. Consequently, in the majority of cases weld metal and base metal are structurally different and have different properties.

(*b*) The adjacent to the weld metal base metal undergoes some uncontrolled heat treatment caused by the secondary heat effect.

(*c*) There are some secondary changes during welding, like mutual diffusion between weld and base metals, absorption or release of gases, etc.

Good weldability of an alloy would require weld and parent metal properties as similar as possible and the least possible change due to secondary heat in the heat-affected zone.

When discussing the weldability problems of aluminum alloys, the following should be taken into account: porosity, cracking, corrosion, strength and physical and physical-chemical properties.

III. POROSITY*

1. *General*

Porosity is not a problem in weldability of pure or relative pure aluminum. This, however, is not the case with aluminum alloys. In spite of quite voluminous literature published on the subject, especially in Europe, the causes and mechanism of porosity is far from being sufficiently explored. As usual, there exists some disagreement among research workers especially in relation to the significance of porosity to physical properties of the weld. The author also is not aware of any systematic work on the influence of porosity on the resistance of the weld to corrosion.

2. *Causes of Porosity*

Hydrogen seems to be widely accepted as being primarily responsible for porosity.[2–10, 16, 17] Other gases, like nitrogen, oxygen, etc., are considered to have insignificant, if any, influence on porosity.[2, 7, 13, 18]

(*a*) *Solubility of Hydrogen.* The solubility of hydrogen in pure aluminum depends on the temperature and on the physical state of the metal. Eborall and Ransley[2] give the solubility of hydrogen in pure aluminum as 0.05 cc/100 g at 500° C and Parker[3] even as high as 100–200 cc/100 g of aluminum. The most complete data, however, have been given by Ransley and Neufeld[4] (Fig. 2).

As may be seen, the solubility of hydrogen in pure aluminum in the solid state is small. However, it suddenly increases in the molten metal and rises sharply with further increase in temperature. It should, however, be emphasized that these values concern the solubility of hydrogen in *pure aluminum* heated in *pure hydrogen atmosphere at 1 atm pressure*. This solubility will be increased as the pressure increases.

3. *Sources of Hydrogen*

Hydrogen in weld may come from two sources:

(*a*) From the alloy itself where it enters during alloy fabrication and processing.

(*b*) From welding.

(*a*) *Hydrogen from the Alloy.* The conditions which affect the hydrogen content in the alloy have adequately been described by many authors.[8–10]

(*b*) *Hydrogen from Welding Operation.* The content of hydrogen which may enter an alloy during welding depends on:

1. The temperature of the weld metal and the period of time the metal is kept at the temperature.[10, 11] The higher the temperature and the longer the time period the more hydrogen one may expect. This may be correlated with the speed of welding.[12]

2. Moisture from the ambient air.[8, 10, 12] It seems that the moisture content of the welding place may have a considerable effect on the amount of porosity in the weld. Eastwood[7] states that in summer time the atmosphere normally contains 6–7 grains of moisture per cubic foot.

3. Moisture from the aqueous flux paste.[8]

4. Moisture from the filler metal.[14, 15]

* More discussion on porosity will be continued in another paper entitled "Hydrogen vs. Acetylene, vs. Inert Gas in Welding Aluminum Alloys."

5. Moisture from the combustion products.[8, 12] It should not be forgotten that one-third of the combustion products of the oxy-acetylene flame consists of water vapor and oxy-hydrogen flame yields even 100% of water vapor.

6. Moisture from the hydrated oxide film.[8, 12]

7. Moisture from surface impurities[7, 8] such as wet corrosion products, wet dirt or even from mechanically adherent water.

8. Moisture from a wet inert gas.

4. *Mechanism of Porosity Formation*

(*a*) *Aluminum, Magnesium, Calcium-Water Reaction.* Aluminum, magnesium and calcium (the latter being an impurity) have a higher chemical affinity to oxygen than hydrogen to oxygen. Magnesium reacts with oxygen or water practically at any temperature ranging from room to magnesium boiling point and above.

The free energy of formation and the difference of the free energy between that of water and of aluminum, magnesium and calcium oxides decrease with increasing temperature.[17] This may suggest that chemically the affinity of these metals to oxygen decreases and, consequently, the reaction between water and aluminum, magnesium and calcium slows when the temperature increases. The opposite, however, takes place. With rising temperature there is more atom mobility and the reaction proceeds progressively faster.

A film, mainly composed of magnesium hydroxide, is formed on a fresh magnesium surface exposed to the atmosphere. Although the reaction is very slow and self-stopping it suggests that magnesium decomposes water even at room temperature.[18] Also, a 0.032 strip of magnesium ribbon when placed in saturated steam at 350° F will be converted to magnesium hydroxide within an hour.[18]

Eborall and Ransley[2] state that:

1. The water vapor-magnesium reaction between solids and liquids is very violent. The metal blackens; consequently, magnesium oxide is formed. Large quantity of hydrogen is involved.

2. At 400° C the reaction is less violent.

de Brouckère[19] found the following films on an Al – 8% Mg alloy:

1. At room temperature on abraded alloy: a film less than 100 Å thick.

2. Alloy heated between 120 and 350° C: a crystalline Al_2O_3 film.

3. Alloy heated above 350° C: MgO film.

4. Progressively heated to 400° C: a duplex film: MgO superimposed on Al_2O_3.

5. Melted and solidified: thick MgO film.

(*b*) *The Actual Probable Mechanism of Porosity Formation.* The mobility of atoms increases as the temperature increases. It is probable that with increasing temperature ions of Mg^{2+} and Ca impurity migrate through the film to the surface and react with the atmospheric oxygen. If, however, the atmosphere is humid or if flux aqueous paste is used, these elements react with water, yielding a high concentration of atomic hydrogen on the surface. The aluminum oxide film does not provide a sufficient barrier against magnesium ion penetration. The atomic hydrogen may exert a pressure equal to several hundred atmospheres[7, 9] and probably diffuses violently into the metal. This may create a concentration of hydrogen in the alloy much higher than indicated in Fig. 2. Hydrogen during cooling migrates from the solid phase toward the remaining liquid phase[4] which, in turn, gathers among the dendritic spaces.[12] When the liquid phase reaches the saturation point in hydrogen, the latter may either evolve toward the surface or precipitate in the molecular form. There must be a violent precipitation of molecular hydrogen, especially on grain boundaries (Fig. 3), when the liquid phase solidifies, because of a sudden drop of hydrogen solubility at the solidification point (see Fig. 2).

Interdendritic voids or even cracks may be formed if the concentration of hydrogen is high enough. It should not be forgotten that the pressure of the precipitated molecular gas is much higher than that of the atomic gas. Ransley and Neufeld[4] assume the pressure in the pores at the time of their formation to be 1–1½ atmospheres.

When such solidified alloy is hot worked these voids may partly be welded and some hydrogen lost by effusion. However, even these partly welded blowholes may be the place where newly diffused hydrogen, during subsequent processing (i.e., heat treatment or welding), may preferably precipitate.

Also, the concentration of hydrogen may be expected to be much higher than indicated by Fig. 2 in the solid heat-affected zone of the weld due to this high atomic hydrogen pressure when water vapor is dissociated. When this high pressure is removed, the dissolved atomic hydrogen may precipitate in the molecular form, preferably into any voids or intercrystalline or interstitial discontinuities.

(*c*) *Magnesium Content and Porosity.* In the case of aluminum-magnesium alloys the proportion of void area to total area increases with the increase of magnesium content.[7, 8, 13, 20] This has also been confirmed by the author's experiments. The amount of voids and welded light-gage sheet increases in the following sequence: 28, 618, 248, 528. One may easily check that these alloys contain 0, 1, 1.5 and 2.5%, respectively, of magnesium.

(*d*) *Impurities Content and Porosity.* Some impurities also seem to increase porosity. This especially has been con-

Fig. 3 Section of an overheated 528-O weld. E, × 5

Oxy-hydrogen weld, 528 filler metal. (*Upper*) Weld metal. (*Lower*) Base metal. Note the distinctly visible fusion zone, a heavy high-temperature crack and weld metal porosity (primarily on dendritic boundaries).

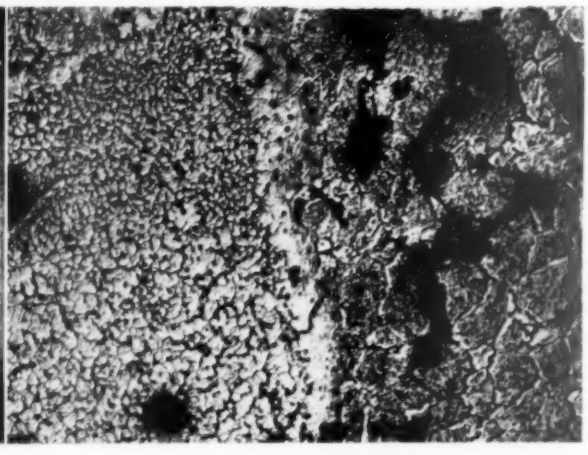

Fig. 4 Typical porosity in an overheated weld. E, × 75

Oxy-hydrogen weld, 528 filler metal. (*Left*) Weld metal. (*Right*) Base metal. Note practically no porosity in the fusion zone.

firmed in relation to calcium.[5, 9, 12] The action of calcium is probably similar to that of magnesium.

(*e*) *Initial Gas in the Alloy Not Necessary to Blister Formation.* Liddiard[9] states that the initial presence of gas in the metal is not essential to produce voids during processing involving heat. He says that Al-Mg sheet originally without gas, covered with flux and heated until the flux is partially melted, always develops blisters. (If the flux was dry this would indicate that presently used fluxes do not provide sufficient protection against atomic hydrogen penetration.)

NOTE: Some authors[5, 9] state that traces of hydrofluoric acid (HF) in the atmosphere prevent the formation of porosity.

5. Weld Metal Porosity

(*a*) *Porosity Is the Function of Hydrogen Concentration.* This seems to be obvious. Ransley and Neufeld[4] indicate that this function is linear at least for sand cast commercially pure aluminum. They further state that 1 cu cm of porosity is produced by 0.46 cu cm of hydrogen at room temperature or 1% increase in porosity corresponds to 0.46 cu cm/100 g increase of hydrogen content. Besides, porosity does not appear in sand cast commercially pure aluminum until the gas concentration reaches 0.12 cu cm/100 g. The difference between the equilibrium solubility and the actual gas content obviously stays in the supersaturated solid solution.

(*b*) *Overheating a Governing Factor.* Disregarding accidental blowholes, it seems that porosity in the weld metal is present only when the alloy has been overheated, i.e., slowly welded or rewelded.[8, 11] Figure 3 and Fig. 4 (left) show such an overheated weld made on 528-O alloy with 528 filler metal. There are two sorts of porosity: globular and angular, with the former being predominant. The former seems to be formed high above solidus and the latter one at or just above solidus.

(*c*) *Filler Metal—A Source of Hydrogen.* The filler metal may be a source of hydrogen and consequently of porosity, especially if not properly cleaned before welding.[15, 16]

(*d*) *Liquidus-Solidus Range.* Eastwood[7] states that the higher the liquidus-solidus range the more porosity the alloy contains. This is because the higher the range, the longer the solidification lasts and, consequently, the larger and the more complex the primary dendrites are. As a result, more gas is trapped in between the dendritic branches.[13]

(*e*) *Porosity Concentrates Toward the Surface.* Blowholes grow in size and are more numerous toward the surface of the weld.[13] This also seems to be simple as the surface of the weld is the hottest and, consequently, dissolves a higher amount of gas than the alloy farther from its surface. Also, gas has a natural tendency to diffuse toward the surface.

(*f*) *Inert Gas Welding.* Although weld metal porosity in corrected gas welding is generally absent, it is very often present in inert gas welding (Figs. 5 and 35). This is probably due to the lack of protection against gas given by the flux in gas welding at the root side.

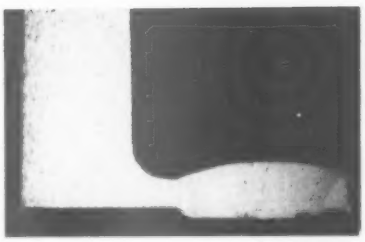

Fig. 5 Porosity in an inert-gas-weld metal. × 3

(*Left*) 61 S-T6 alloy boss. (*Right*) 528-O alloy sheet. 528 filler metal. Note porosity at the root of the weld metal.

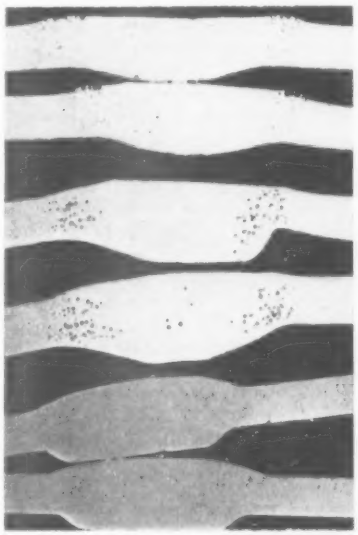

Fig. 6 Heat-affected zone porosity, blistering and swelling. × 3

528-O alloy sheet, 528 filler metal. (*Upper*) Oxy-hydrogen weld. (*Middle*) Oxy-acetylene weld. (*Lower*) Inert-gas (argon) weld.

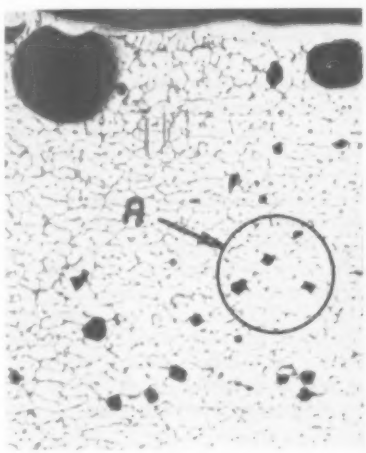

Fig. 7 Weld metal and heat-affected zone porosity. D + E, × 100

Clad 24S-T3 alloy sheet, oxy-hydrogen weld, 24S filler metal. Note absence of porosity in the cladding and in the fusion zone.

6. Heat-Affected Zone Porosity

(*a*) *General.* Heat-affected zone porosity, blistering and swelling are treated together because it is believed that they all have a common cause—gas. Blistering is a superficial porosity which breaks the surface because of the increased gas pressure during welding.[10] Swelling almost always accompanies porosity in gas welding and it has seldom been observed (by the author) in inert gas welding. However, sometimes there is a swelling without the apparent presence of porosity (see Fig. 34, middle specimens). "Apparent" is emphasized, because porosity may be of such a fine nature that normal microscopic magnification may not detect it or it may be confused with heavier grain boundaries precipitation.

(*b*) *Mechanism of Porosity.* Some authors blame initial gas content of the alloy for porosity. Others, however, do not agree with this theory.[5, 8, 12] Liddiard[9] even states that initial gas content is not necessary to produce porosity.

General mechanism of porosity seems to be the following. When an alloy is solidified there is some amount (depending on alloy content and speed of solidification) of higher melting point compounds or impurities which precipitate on the grain (or dendritic) boundaries.[9, 10, 15, 2] During hot and cold working these precipitates are elongated, broken into small fragments, and arranged into stringers.[10, 13] These fragments may produce discontinuities in which gas precipitates. This gas may come from the alloy itself or from moisture during welding. However, it seems that precipitates are not necessary for occurrence of porosity. Any other discontinuities and even grain boundaries may act as such. Some published photographs,[8, 15, 16] and Figs. 3, 4, 6–8 suggest that the heat-affected zone porosity is rather arranged at random. Of course, any new hydrogen diffused during welding will preferably precipitate in already existing voids or blowholes or in some other discontinuities or even on the grain boundaries and increase the internal pressure (Fig. 8).

(*c*) *Blistering.* Some authors have reported blistering only on the root side of the weld,[10] others on both sides.[8] The author's experience, however, shows blistering present only on the face side of the joint (see Figs. 6, 7 and 33).

(*d*) *Amount of Porosity.* The amount of porosity depends on a few factors:

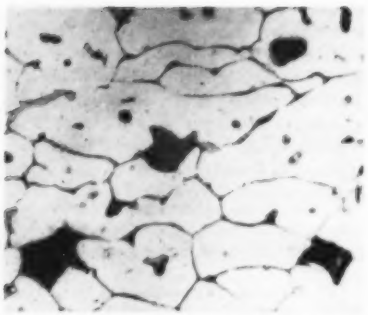

Fig. 8 Detail A from Fig. 7 at higher magnification. D + E, × 300

Note the location of the blowholes on the grain boundaries, their irregular shape and a globular blowhole inside the grain. No porosity in the fusion zone.

1. Thickness of the Welded Part. The thicker the part the more porosity will be present, all other conditions being the same.[8, 11, 12] This is because thicker parts require slower welding and, consequently, gas has more time to diffuse and to precipitate.

2. Magnesium Content. The more the magnesium and alloy contains the more porosity will be produced during welding.[8, 11, 15, 20]

3. Speed of Welding. The higher the speed of welding the less porosity is produced.[8] It also partly explains why porosity occurs in a decreasing amount in oxy-hydrogen, oxy-acetylene and inert gas welding. Rewelding (e.g., during repair) or when welds intersect also produces increased porosity.[11] Figure 3 shows a plane section of the weld crack from Fig. 24. This weld was very strongly overheated due to the junction of two heavy welds and slow welding.

(*e*) *Location of Porosity.* This author's experience shows that the porosity is mainly located in the heat-affected zone and, so far, has not been spotted in the fusion zone itself (see Figs. 3, 4, 7, 8 and 9). Even when a particular spot of the fusion zone seems to contain porosity, the higher magnification shows that this is not so (Fig. 9). Porosity in the oxy-hydrogen weld (Fig. 6) appears to be the heaviest at the surface and on the face side of the weld and seems to produce blistering and swelling.

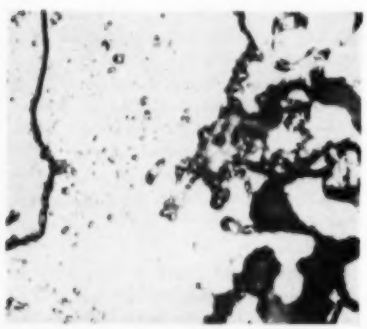

Fig. 9 Fusion zone of an overheated oxy-hydrogen weld 52S-O alloy sheet (52S filler metal). D + E, × 300

Note absence of porosity or cracking in the fusion zone. (*Left*) Heat-affected zone microcracks. (*Middle*) Fusion zone. (*Right*) Weld metal porosity and segregation.

In the case of the oxy-acetylene weld porosity is rather confined to the center of the specimen and, although it produces swelling, it does not give blistering (Fig. 6). This seems to be confirmed by photographs by Erdmann-Jesnitzer[11] and up to some extent by Pendleton.[8] Inert gas weld shows sometimes some light swelling but does not, at least in the author's experience, show blistering and heavy porosity. This general trend has been observed by the author in welding of thin gage clad 24S-T3, 24S-T3, 52S-O and 61S-T6 alloys.

However, does the obvious absence of porosity or microporosity in the heat-affected zone of an inert gas weld mean that the weld is porosity free? The author's limited experience with corrosion of such welds seems to give a negative answer to this question.

(*f*) *Shape of Porosity Voids.* Only porosity voids close to the surface (see Figs. 6 and 7) or those inside the grains (see Fig. 8) in a normal weld or those in overheated welds have more or less regular globular shape. This porosity is probably formed either at a higher temperature or where there was little resistance to perfect void formation. Other porosity voids have either an irregular-angular form (Figs. 7 and 8) or even the form of heavy cracks (Fig 4). The former is created in the grain interfaces (Fig. 8) or other discontinuities and the latter produces grain separation and may be found only in the overheated welds (Fig. 4).

7. How to Avoid Porosity

Porosity may be avoided or decreased in intensity during alloy manufacturing or during welding. The available literature[3–7, 9–13, 20] clearly indicates how to avoid porosity during an alloy fabrication.

There may exist another possibility of decreasing the free gas content as suggested by the author in relation to decreasing crack sensitivity in welded steels,[21] namely the addition of rare earths. Some rare earth elements like Lanthanum, Cerium and Praseodymium may dissolve a very high amount of hydrogen and, what is even more interesting, this solubility increases with decreasing temperature.[22]

During Welding. (1) Use clean and dry sheets and filler metal.[9] (2) Avoid welding in a humid place.[8] (3) Weld as fast as possible.[9, 11] (4) Avoid the use of flux paste made with water.

8. Significance of Porosity

Some authors seem to neglect or to minimize the consequence of porosity. Others, however,[7, 8, 11, 12] are of the opinion that porosity decreases strength properties of the weld and especially its ductility.[7, 8] Impact and fatigue are also adversely affected.

(*a*) *Static Strength.* Pendleton[8] states that the tensile strength of a weld decreases with increasing thickness of a plate and with increasing porosity. The yield strength and elongation also suffer.[7]

(*b*) *Impact.* Impact strength decreases with increasing porosity.[7]

(*c*) *Fatigue.* Eastwood[7] indicates that fatigue is very adversely affected by porosity. This has to be expected especially in the case of bending fatigue. The strength of the outer fiber is decreased by the notch effect produced by blistering. On the other hand the gas pressure inside porosity voids may also decrease the endurance limit.

(*d*) *Cracking.* The author is of the opinion that gas pressure, especially at the moment of porosity formation, increases the crack sensitivity of an alloy.

(*e*) *Tightness.* From the tightness point of view globular porosity is not as bad as angular.[7] The heat-affected zone porosity, shown in Fig. 4 which represents a plane section of the crack from Fig. 24, showed lack of tightness when tested with dye penetrant but not with fluorescent penetrant. The lack of tightness is especially disadvantageous in reservoirs.[11]

(*f*) *Corrosion.* There is evidence that porosity adversely affects resistance to corrosion. This problem will be dealt with in more details in Section VI.

IV. CRACKING

1. General

It is easy to become confused when reading papers on the crack sensitivity of aluminum alloys, since often the authors fai to make clear whether they are discussing cracking of the weld metal or of base metal. There is an apparent disagreement or contradiction when an author states in one place that the crack sensitivity of an alloy is greater with increasing and in another—with decreasing alloy content. The authors concerned were right in both cases, only on one occasion they spoke about the heat-affected zone or subsolidus weld metal cracking and, in another, about weld metal cracking. Other authors want to differentiate between a shrinkage cavity and a crack. Well, for all practical purposes in the case of welding, both are cracks, except that they occur at different temperature levels and by a different mechanism. There is also some tendency to generalize test results. It should, however, be borne in mind that there is not and cannot be a universal alloy which would give good results in welding of both thin and thick sections. The service application of an alloy must also be taken into account. An alloy may be good for one application and completely unsuitable for another.

2. *Causes of Cracking*

A crack is produced because an internal stress is created which may be due either to the decrease of volume of a cooled alloy as a whole, or to differential shrinkage of its particular phases, or to the increased internal pressure of precipitated gases.

It seems that gases are not given sufficient attention in weld cracking. Some research workers even state that there is no correlation between porosity and cracking. This should rather be stated in the following manner. It does not mean that gas does not help in producing cracking if there is no porosity around a crack. Gas pressure may be present in an alloy even if there is no apparent porosity visible under the microscope. The porosity may be submicroscopic or it may be confused with the alloy constituents.

3. *Weld Metal Cracking*

(*a*) *Mechanism of Cracking.* In order to understand the present theory of the mechanism of cracking one must see how an alloy is solidifying. For this purpose let us consider a hypothetical binary alloy system A–B with boundary solutions α and β and an eutectic E (Fig. 10). Let us further take an alloy containing $x\%$ of the metal B. An alloy containing $x\%$ of the metal B would solidify in such a manner that its liquid phase concentration in B would follow *a*, *c*, *e* (liquidus) and the solid phase, *b*, *d*, *f* (solidus) provided the solidification takes place in a condition of perfect equilibrium. The solidification of this alloy would end at *f* at a temperature t_5. The first nuclea of grains appear when the line representing the alloy concentration intersects liquidus. The grains during solidification would develop from nuclea into dendrites. These dendrites would progressively develop branches which intermesh. Among the branches liquid phase and impurities might be trapped. As the dendrites shrink during cooling and, where there is not enough liquid to "heal," they might separate, producing "hot tears" or "hot cracks." French researchers call the temperature interval at which this cracking occurs "Intervalle de Fragilité a Chaud" ("The Interval of Hot Brittleness").[28] This hot tearing under equilibrium conditions should be the most severe in an alloy of the composition x_3, i.e., when the liquidus-solidus temperature interval and, consequently, the solidification period is the largest. This would be because the dendrites would have the maximum time to grow and shrink.

This ideal equilibrium solidification, however, never takes place in practice. The diffusion of the metal B into grains which appear at *b*, *d*, etc., is slow. As a result the centers of grains are richer in metal A and the peripheries are richer in metal B. This phenomena is called "coring." As a further result of this, the liquid phase is not in equilibrium with the solid and contains more and more metal B and the mean composition of the solid phase does not follow *b*, *d*, *f*, i.e., the solidus but b_1, d_1, f_1, h_1. The final solidification of the alloy takes place at a temperature t_4 and not t_5 and the final liquid phase composition correspond to *g* and not to *e*.

If the cooling is fast enough, the final solidification may take place at t_E temperature (eutectic), where the concentration of the liquid phase reaches the eutectic compositions. It solidifies the latter and

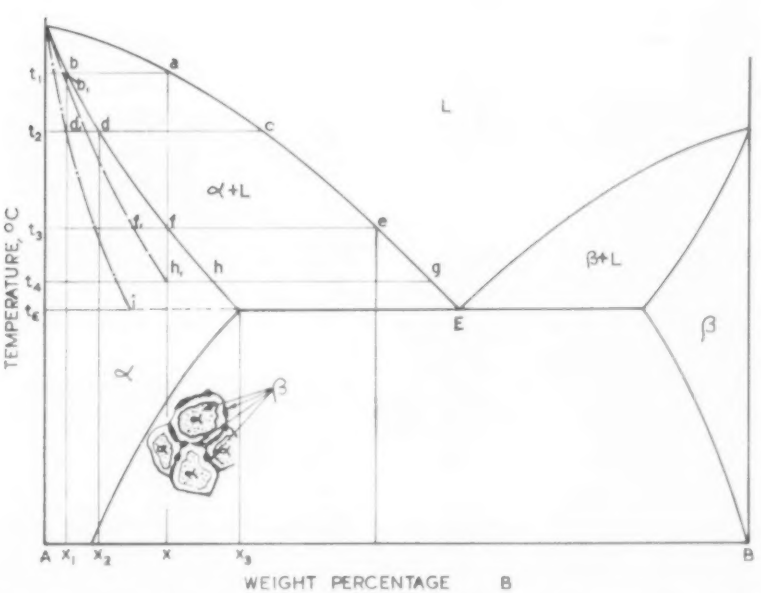

Fig. 10 Coring effect and precipitation of β-phase on the grain boundaries

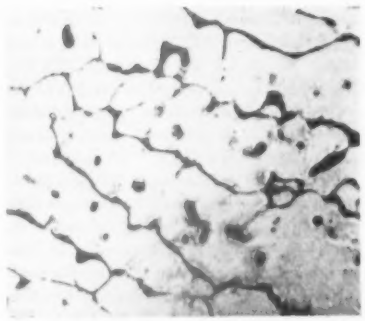

Fig. 11 Weld metal structure. D + E. × 300

Oxy-hydrogen welded 248-T3 alloy sheet, 248 filler metal. Note heavy interdendritic film.

provides "healing" to possible cracking,[8] i.e., it fills cavities formed among dendrites due to shrinkage. The eutectic is being made responsible for hot crack prevention. In fact this "healing" is probably accomplished by the liquid phase throughout the whole solidification and not only by the eutectic.

Figure 11 shows a weld metal structure produced with 248 filler metal. Some eutectic may be seen among the dendritic branches. Also there are signs of coring.

According to present knowledge, it is considered that the eutectic in the weld metal is necessary to prevent cracking.[8, 9, 20, 24, 28]

(*b*) *Severity of Cracking.* Theoretically the alloy most sensitive to weld cracking would be that which possesses the largest liquidus-solidus range (the greatest solidification temperature interval), i.e., an alloy corresponding to the highest solid solubility (e.g., $x_3\%$ *B*, Fig. 10).[8, 24, 27] This, as previously stated, would take place only if the alloy would solidify at a perfect equilibrium rate. This, however, never happens in practice.

In practice the rate of cooling is such that equilibrium is never reached with the exception, of course, of pure metals or of metals with insignificant alloy content.

Eutectic Content: In welding aluminum alloy weld metal cracks start declining with the first appearance of the eutectic.[24, 28] Jennings, Singer and Pumphrey[20] give the critical amount of eutectic needed in order to practically prevent hot cracking in Al-Si, Al-Cu and Al-Mg alloys as 23% by volume.

Alloy Content: Pure or commercially pure aluminum can be welded or cast without cracking. However, the first addition of alloy such as magnesium, copper or silicon increases the freezing range of the alloy and thus increases its crack sensitivity, which reaches some maximum after which it declines.[8, 9, 20, 24, 28, 30, 31] This corresponds to the first appearance of the eutectic.[24, 28] With further increase of alloy content, the crack sensitivity further decreases and reaches a minimum and usually levels off. The percentage of alloy, at which there is the

maximum crack sensitivity, is given as about 0.4%[20] to 0.6%[21] Si in Al-Si alloys, about 1%,[21] 1.5%[20] to about 3%[10, 28] Mg in Al-Mg alloys and about 1.0%[20] Cu in Al-Cu alloys. Some authors have found that the addition of some balanced proportion of copper,[10, 27, 28] silicon,[10, 11, 20, 21, 26–28, 35, 21] maganese,[28] iron[10, 28] and titanium[10] decreases the crack sensitivity of Al-Mg alloys. This may be due to the decrease of the main alloy solubility, thus producing more eutectic, such as due to the addition of Fe, Si, Mn,[28] Si and Cu[27] or by grain refining by addition of titanium.[10, 29] The alloy addition, however, must be suitably balanced, otherwise there may be a second maximum crack sensitivity as in the case of Al-Mg containing manganese.[10] Erdmann-Jesnitzer[11] proposed such a balanced alloy for weldable light-gage alloy. This had the following composition:

Mg, %	3.20–3.80
Si, %	0.50–0.80
Mn, %	0.30–0.60
Fe, %	0.50 max
Cu, %	0.10 max
Zn, %	0.20 max

That alloy apparently gives porosity- and crack-free weld metal and heat-affected zone.

Impurities, such as arsenic and calcium, increase cracking. This, according to Lees,[27] may be due to an increase of the freezing range or due to formation of a thin (brittle ?) film, as in the case of arsenic. It seems that the destructive action of calcium may also be due to the tendency of an alloy, containing this metal, for the high gas absorption which, in the author's opinion, may help crack formation.

Pre- and Postheating: Dowd[30] has found that preheating and postheating decreases cracking of a heavy sheet weld. The author's experience also indicates that the preheating does reduce crack sensitivity, especially in the beginning of welding.

Grain Size and Size and Shape of Dendrites: Generally, authors[24, 27, 21] agree that the larger the grain size and the larger and the more complex the dendrites the more sensitive is the alloy to cracking. This is probably because the larger the grains and dendrites are, the thinner is the film envelop[27] which thus cannot accommodate the stresses. Complex dendrites on the other hand may trap liquid in between their branches and prevent its healing action.

Plate Thickness: Thick plates are more sensitive to cracking than thin ones.[8, 14, 28] This may be due to two factors:

1. The thicker the plate, the longer welding lasts and the less eutectic there is during the final freezing of the weld (the freezing occurs in conditions closer to equilibrium).

2. The longer time of welding of a thick plate gives more opportunity for gas to dissolve and thus, there is more precipitated gas porosity which, in the author's opinion, helps produce cracking.

Time and Temperature: The longer the time of welding and the higher the temperature the more the weld is sensitive to cracking.[24]

(*c*) *Temperature of Cracking.* Many authors[10, 20, 25, 27, 28] think that cracks in solidified casting or weld metal are "hot tearing" and that they are produced above solidus. Others,[23, 29] to which group this author also belongs, believe that the weld metal may crack either above or below solidus. The latter occurs in a sudden manner,[28] like small explosions. Pumphrey and Moore[23] also state that a weld metal with low alloy content is sensitive to cracking above solidus and one with high alloy, below solidus. The formation of high-temperature cracking has already been explained. Below solidus cracks are probably due to the decrease of grain boundary strength caused by the depletion in alloy of the grain outer crust on cooling.[23] The author also believes that there may also be a third temperature of cracking just below the solidus. When the weld containing high-gas content solidifies, the gas from the solid phase migrates to the liquid phase as the latter has higher gas solubility. Consequently, on solidification, the precipitating gas may create such a pressure that the brittle intergranular eutectic may crack.

(*d*) *Types of Cracking.* There seems to exist a prevalent opinion that weld metal cracks are intergranular.[10, 20, 25, 27]

This author's experience seems to indicate that there are two types of cracking: inter- and transgranular cracks. The intergranular cracking may occur either above the solidus and it is primarily the so-called "hot-tearing" or "hot-cracking," or at or just below the solidus. An example of the former is given in Fig. 12.

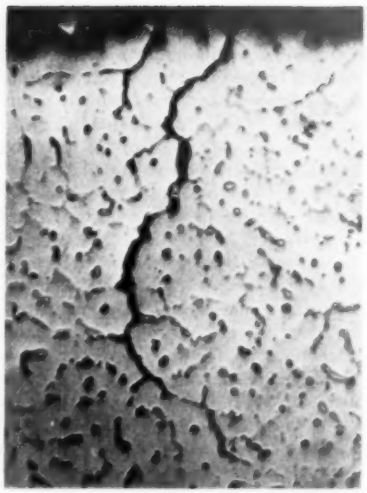

Fig. 12 Above solidus intergranular microcrack in the face of the weld (24S-T3 alloy sheet, oxy-hydrogen weld, 24S filler metal). × 250

An example of a subsolidus crack is given in Fig. 13. This is an inert gas flange weld between 61S-T6 boss and 52S-O sheet. The crack was produced between a lack of fusion and a blowhole. Portion A of the crack is interdendritic.

The author believes that there also may be low-temperature (room temperature) cracking of the weld metal. This cracking may be inter- or transgranular. The microcrack *B* (Fig. 13) probably occurred at room temperature, when the small section of the alloy between the end of the lack of fusion and the end of the crack *A* could not support the high shrinkage stresses. This portion of the crack seems to be transgranular.

(*e*) *"Crazing."* Pendleton[8] makes a very interesting observation about small multidirectional cracks which he calls "crazing." He states that crazing was confined almost exclusively to the root of

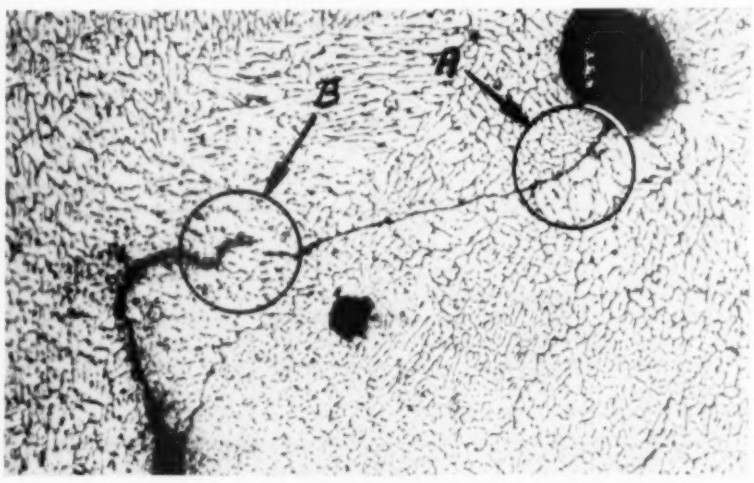

Fig. 13 Inert-gas-(argon)-weld metal crack. Flange welded 61S-T6 boss (left) to 52S-O alloy sheet (right). D + E, × 100

the weld and occasionally extended to the heat-affected zone. He also suggests that "crazing" may be connected with internal porosity and may be produced by the escaping gas, which forces aside the thin film of liquid between the grain boundaries and lifts up the grains. This is a very important observation. The last part of Pendleton's statement may suggest that "crazing" happens just above or at the solidus.

The author's experience has also produced this type of cracking. It seems to be confined rather to overheated welds. However, this fine multidirectional cracking has been found on both sides in the heat-affected zone (Fig. 14) and only on the face of the weld metal itself (Fig. 16).

The former cracks have in some cases quite an appreciable depth, e.g., the ones shown in Fig. 14 which, after etching for 15 min were still visible (Fig. 15). In another case cracks started disappearing after only 5 min etching.

The face weld metal "crazing" (Fig. 16) seems to be more serious than the one from the heat-affected zone. Figure 17 shows the same weld after etching. Cracks are definitely intergranular. Figure 18 shows similar cracked welds sectioned perpendicularly to the center line of the weld. Almost the entire section of the weld shows these cracks. The cracks from Detail *A* (Fig. 19) follow the grain boundaries and are connected with internal porosity of the weld.

Pendleton's observations have been confirmed even in relation to lifting of the grains (see Fig. 18). The innocent looking "crazing" is not at all so innocent. Here an important remark should be made. This multidirectional cracking could not be detected with fluorescent penetrant method. Dye penetrant, however, showed it very clearly.

(*f*) *Discontinuous Welding.* Dowd in his work[39] states that a discontinuous weld on 52S-O plates with 52S filler metal cracked much more severely than a continuous weld. This also is an important observation. The discontinuous weld, when made by a skilled operator, has a good appearance, especially on steel. Figure 20 shows such an innocent looking but treacherous "beauty." Figure 21 reveals what is left of this deceptive face after its make-up has been cleaned off. Deep gaps among particular pools and small cracks are uncovered. Photomacrograph in Fig. 22 also shows that the internal soul of such weld is not better than its external face. Every pool may distinctly be seen as being separated with porosity and cracks (Detail *B* in Fig. 22). Figure 23 shows Detail *C*, i.e., the junction along the two pools.

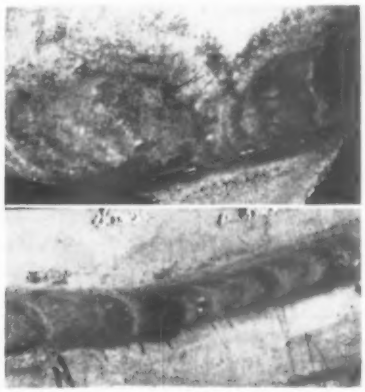

Fig. 14 "Crazing" in the heat-affected zone. × 2

Overheated oxy-hydrogen weld, 52S-O alloy sheet, 52S filler metal. (*Upper*) Root side of the weld. (*Lower*) Face side of the weld.

Fig. 15 "Crazing" from Fig. 14 after 15 min deep etching. A + B, × 2

Fig. 16 "Crazing" in the weld metal (overheated oxy-hydrogen weld, 52S-O alloy sheet, 52S filler metal). × 3

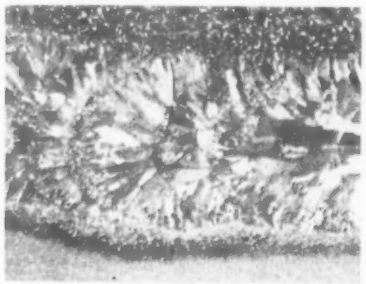

Fig. 17 "Crazing" from Fig. 16 after deep etching. Note cracks following dendritic boundaries. C, × 3

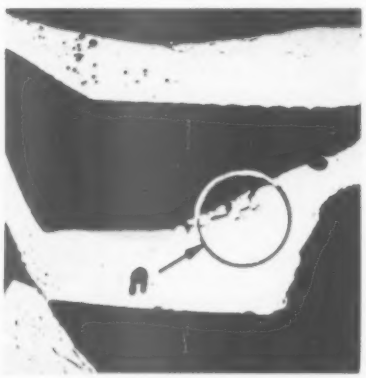

Fig. 18 Section through "crazed" weld metal from Fig. 17. × 5

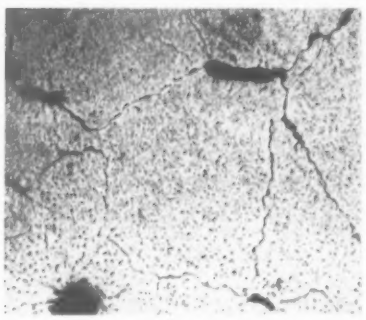

Fig. 19 Detail A from Fig. 18. "Crazing" is interdendritic cracks. D + E, × 50

Fig. 20 Discontinuous weld (52S-O alloy sheet, oxy-hydrogen weld, 52S filler metal). × 1.5

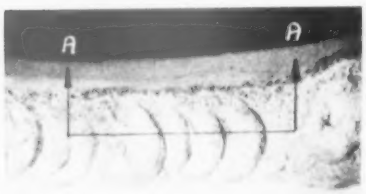

Fig. 21 Same as Fig. 20 after deep etching. Note gaps among particular solidified pools. A + B, × 2

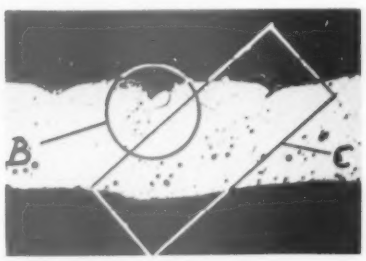

Fig. 22 Sections A-A through weld from Fig. 21. E, × 5

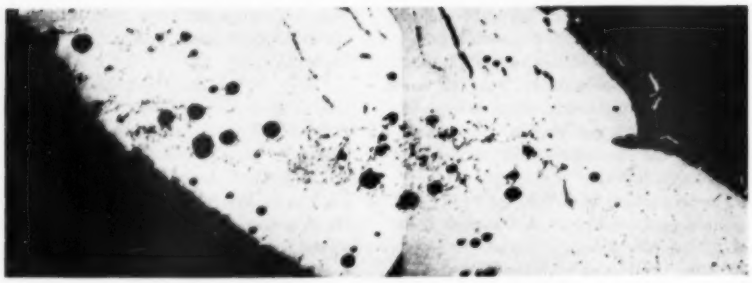

Fig. 23 Detail C from Fig. 22. E, × 15

Fig. 24 High-temperature heat-affected zone crack (overheated 528-O alloy sheet, oxy-hydrogen weld, 528 filler metal). × 2

The entire weld is badly infested with macro- and microporosity and with macro- and microcracks. These are produced by the long exposure of hot alloy to the end of the outer cone of the flame and it should not be forgotten that the end of the oxy-acetylene flame contains one-third of water vapor as the final combustion products and oxy-hydrogen combustion products contain even 100% water vapor.

(g) Remedy. There are numerous remedies which may be taken to avoid weld metal cracking.

Alloy Content: All research workers agree that filler metal used for welding aluminum alloys should contain higher alloy content.[8-11, 13-15, 20, 24, 28-31, 34] The alloy content, however, should be properly balanced especially in the more complex alloys. Also, the addition of refining agents like titanium, zirconium or columbium[29] decreases crack sensitivity.

Speed of Welding: The higher the speed the fewer cracks a weld metal will form.[10, 14] The main advantage of inert gas welding whether with a nonconsumable or consumable electrode lies in the speed of welding.

Cleanliness.

Dry atmosphere of the shop.

Method.

Welding should never be discontinuous.

4. *Heat-Affected Zone Cracking*

(a) Solid Solubility Limits. It is felt that a short discussion of solid solubility limits of some most common elements in aluminum may help understanding of further considerations.

Table 1 gives the solubility of some element in *a* aluminum solid solution.

Of course, every constituent present in the alloy affects the solubility of other constituents.

As may be seen, such elements as magnesium, zinc and copper have a relatively greater solubility in aluminum at high temperature (e.g., at eutectic temperature) than at low, but this solubility quickly decreases when the alloy is cooled. Other elements such as silicon, and especially iron and nickel, have a very low solid solubility. This solubility decreases almost to nothing at room temperature. These alloying elements (or in some cases impurities) have a tendency to precipitate usually as compounds on the grain boundaries or inside the grains. The more complex the alloy the more complex the precipitates. Magnesium and silicon, for instance, form a very, brittle Mg_2Si compound when present in the alloy.

Special attention should be paid to titanium. Although the Al-Ti system has not yet been properly established,[35, 23] it is known that the solidification of Al-Ti intermetallic compound ($TiAl_3$) takes place only a few degrees above the solidification point of aluminum. On the other hand the diffusion of Ti in solid Al is extremely slow. These properties have been exploited by using titanium-aluminum compound as "seeds" to provide nucleation and, consequently, grain refining. Titanium has been used in Europe for manufacturing aluminum filler metal. Commercially pure aluminum, when welded with a commercially pure aluminum filler metal, would show a very coarse columnar structure in the weld metal. However, when a filler metal, containing as little as 0.08% titanium, is used, it would provide a very fine-grained weld metal structure.[32]

(b) Mechanism and Types of Cracking. It seems that there are three types of the heat-affected zone cracks depending on the temperature at which they occur.

High-Temperature Cracks: High-temperature cracks are heavy cracks. There are indications that they occur at or just below solidus.

As was mentioned above, the hydrogen from the decomposing water vapor creates very high pressure which produces high hydrogen solubility in the solid heat-affected zone. When this cause of high hydrogen pressure is relieved, the dissolved hydrogen precipitates into intergranular or interstitial discontinuities or effuses. Precipitated hydrogen exerts high internal pressure which produces separation of the grains and may produce a crack. Such a crack is progressively enlarged as the alloy shrinks. A high temperature intergranular crack is shown in Fig. 24 and its plane section in Fig. 3 (left side). Figure 26 shows another high temperature crack. It is an intergranular crack.

Medium-Temperature Cracks: Pumphrey and Moore[23] and Hollard[28] refer to cracks which occur at lower temperatures. Hollard states that such cracks were observed at about 200° C. Pumphrey and Moore explain this by the weakening of the grain periphery, because of the depletion of its alloying content precipitating on the grain boundary. This seems to be a reasonable explanation. Perryman and Blade[37] give remarkable photomicrographs of a solution-treated and aged Al – 10% Zn alloy. The peripheries of the grains show a definite depletion in zinc. Those depleted and extremely narrow zones Fig. 25 have lower strength than the core of the grains or of the grain boundaries. When stresses are present (e.g., shrinkage stresses) these zones stretch beyond the yield point and

Table 1—Solubility of Elements in Aluminum Solid Solution[22, 23, 35]

	Element											
	Cu			*Mg*				*Si*				
Solubility, %	5.65		0.45	15.35[22]		2.95[22]		1.5[25]	1.65		0.05	
Temp., ° C	548		300	451		150		Room	577		200	
	Zn			*Fe*		*Mn*		*Ni*		*Cr*		
Solubility, %	82.8	78 or 31.6	5.6	0.05	0.006	1.82	0.36	0.03	0.006	0.77	0.07	
Temp., ° C	382	275	125	655	500	658.5	500	640	505	661	400	

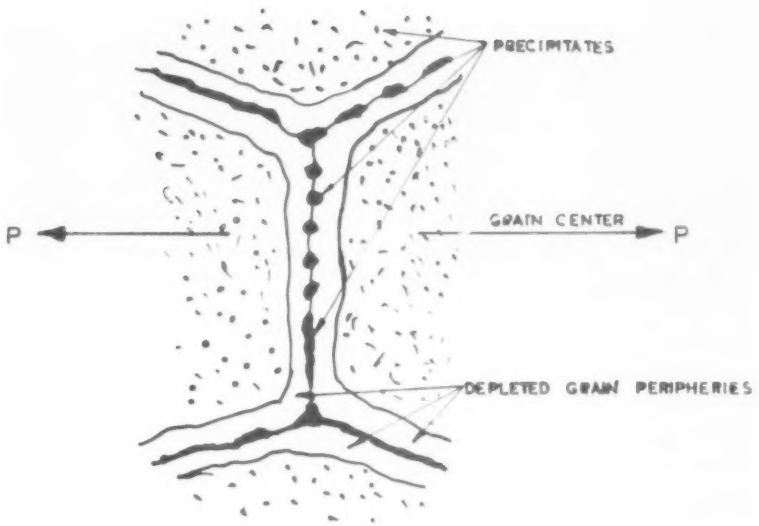

Fig. 25 Effect of intergranular precipitation

crack. Figure 26 gives such, which is believed to be, a medium temperature crack.

Low-Temperature Microcracks: These cracks are produced at low temperatures and occur either between the ends of high or medium temperature cracks or between porosities (Fig. 27) where reduced sections cannot support shrinkage stresses. Sometimes these cracks take the form of "shatter microcracks." Low-temperature microcracks are usually transgranular. They may extend on a considerable distance but are so fine that they may be seen only under higher magnification and after light etching. The internal gas pressure in porosity voids also may help in developing these cracks.

The shatter character of low-temperature microcracks suggests that they are produced rather violently. This is probably true because quite often one may hear characteristic metallic sounds of cracks which follow the welding torch.

(c) *Severity of Cracking.* There are few factors which influence the severity of cracking in the heat-affected zone.

Time: The longer the time the more complete precipitation there is on the grain boundaries and, consequently, the more the outer boundaries of the grains are depleted in alloying constituents. This is why such alloys as Al – 5% Mg crack severely when rewelded (repaired) or where the welds intersect.[11] The author's experience with Hydronalium 5, (Hy-5)[12] showed that alloy was less crack sensitive when fast welded, but that it would crack unmistakably when slow welded or on weld intersections.

Alloy Content: It is generally recognized that Al-Mg alloy up to 4.5% Mg are not particularly crack sensitive.[28] This is true if the weld is made quickly and correctly. However, the higher the alloy content, the more sensitive the material is to cracking.[19, 20]

Some small balanced additions of iron, copper or silicon reduce crack sensitivity.[11] Chromium[29] increases the stability of solid solutions by raising the minimum temperature of precipitation. Titanium, zirconium and columbium produce grain refining and, consequently, reduce crack sensitivity.

Cold Working: Cold working accelerates the precipitation of alloying constituents and, consequently, is detrimental from the point of view of cracking.

(d) *Porosity and Cracking.* There exists a difference of opinion as to whether there is any correlation between porosity, blistering and cracking. Some authors consider that there is no relationship. Hollard,[28] however, states that cracks may affect a blistered area, but that there is no relationship between gas content and cracking. The photograph given by Erdmann-Jesnitzer[11] shows a typical heat-affected zone crack around which there is no porosity visible although the heat-affected zone itself and the weld metal are infested with blowholes and pinholes.

The author's observation suggests that there is some relationship between gas content in the alloy and cracking. Gas may help in producing cracks. It is interesting to note that there is no porosity in the immediate vicinity of a heavy high-temperature crack as shown in Fig. 3. The crack itself, however, seems to pass through several blowholes. It is probable that the actual cause of such a crack may be a high concentration of gas on the grain boundaries which has produced separation of grains and an intergranular crack. Further on, this crack may be used by gas, from the adjacent to the crack area, as an escaping channel. Thus the gas pressure may be relieved and no more pores may be formed. Further shrinkage of

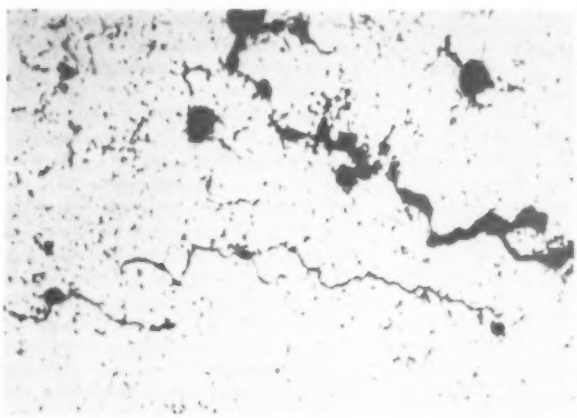

Fig. 26 High- (upper) and medium- (lower) temperature heat-affected zone intergranular cracks (overheated oxy-hydrogen weld, 52S-O alloy sheet, 52S filler metal). D, × 80

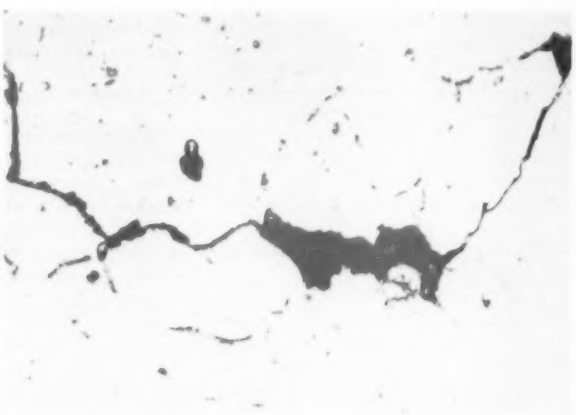

Fig. 27 Low-temperature heat-affected zone transgranular microcrack between porosities (overheated oxy-hydrogen weld, 52S-O alloy sheet, 52S filler metal). D, × 400

the metal during cooling may have enlarged this crack.

(*e*) *Remedy.* The following steps may be taken to avoid or at least decrease cracking in the heat-affected zone.

1. Use a rather low alloy content. This is contrary to the requirement for weld metal.

2. Use an alloy with more complex but balanced alloy content such as that previously given and as proposed by Erdmann-Jesnitzer.[11]

3. Weld as quickly as possible. Inert gas as a fast method of welding is better than oxy-acetylene and this, in turn, is better than oxy-hydrogen.

4. Avoid using oxy-hydrogen welding because it is slow and because the flame contains 100% water vapor as the final combustion product.

5. Avoid welding in a humid place.

6. Use clean and dry filler metal and base alloy.

7. Avoid designing intersecting welds.

V. GRAIN GROWTH AND RECRYSTALLIZATION

Sheets are stretched or bent after final heat treatment for strengthening. This induces some cold working of the alloy. Grains, deformed during cold working, recrystallize during heating. After, however, some percentage of cold working is reached, the grains have a tendency to grow. Pure metals or alloys of the solid solution types, such as Al-Mg alloys, are especially sensitive to grain growth. There is a critical amount of cold working and a critical temperature to which the alloy may subsequently be heated and at which there is a maximum of grain growth.

Bungardt and Rohde[32] made extensive studies on the recrystallization of Hy-7 (6.5% Mg, 0.27% Mn, usual percentage of Fe and Si, balance Al). The mean grain size of the material before cold reduction was 600μ^2. The critical deformation by cold reduction was found to be 3–6%. The grain size of the subsequently heated alloy increased up to about 400°C., at which temperature there was a sudden decrease. Further increase of the temperature continued to provide a steady and increased growth of grains. The investigators found the mean grain size after 1 to 3 hr at 500° C to be about 80,000 to 180,000 μ^2 and at 550° C, 210,000 to 700,000 μ^2.

When an alloy in cold worked condition is welded, the temperature rises to a critical level somewhere in the heat-affected zone and recrystallization occurs. Figure 28 gives an original microstructure of a 24S-T3 sheet. The grains are elongated due to cold working. Figure 29 shows the heat-affected zone microstructure after oxy-hydrogen welding. The grains have recrystallized and increased in size and, in addition, heavy precipitates, mainly $CuAl_2$ are visible on the grain boundaries.

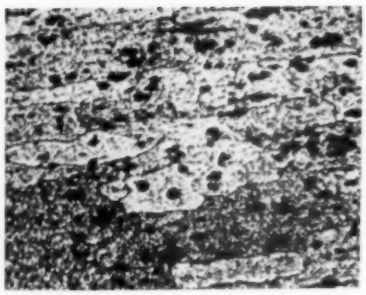

Fig. 28 Microstructure of 24S-T3 alloy sheet. D + E, × 300

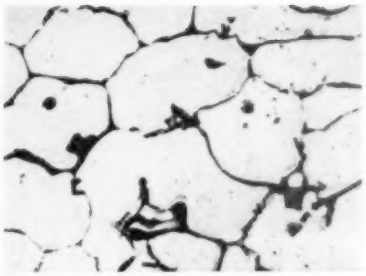

Fig. 29 Same alloy as in Fig. 28 recrystallized in the heat-affected zone (oxy-hydrogen weld, 24S filler metal). D + E, × 300

The author's own experience with 2-mm Hy-5 alloy sheet was also interesting.[52] The investigated alloy was in a slight cold worked condition. The mean grain size was 325 μ^2. After welding with oxy-acetylene, the heat-affected zone grain recrystallized and its size increased to 32,000 μ^2 and in places where the welds intersected, the grain size increased to as much as 62,000 μ^2. The recrystallization process, however, was not yet accomplished, because the time the alloy was kept under the heat was too short to produce an equilibrium. Of course, Hy-5 (Al – 5% Mg) is more critical from the grain growth point of view than Hy-7 because the former has a lower Mg content than the latter.

The percentage of cold working of an alloy to be welded must be strictly controlled in order to avoid excessive grain growth.

The author is not aware as to whether there has been any extensive investigation on the significance of grain growth during welding to strength and physical-chemical properties. The following effect, however, seems to be without doubt. The grain growth in the heat-affected zone would: (*a*) Decrease the ductility of the joint. (*b*) Reduce yield strength, impact and endurance limit. (*c*) Decrease resistance to corrosion especially to stress and intergranular corrosion.

VI. CORROSION*

1. General

Electrochemical reaction seems to be widely accepted today as the cause of corrosion. As is well known, two factors are to be present in order to produce corrosion: a potential difference and an electrolyte. An electrolyte is present practically everywhere; its simplest form being common moisture.

Surface corrosion is the least dangerous as it attacks an alloy uniformly and, consequently, is relatively easily detected and prevented. The most dangerous types of corrosion are internal: stress, intergranular and fatigue corrosion. These types of corrosion are rather concentrated and difficult to spot and to prevent. Therefore, it is proposed to pay more attention to stress, intergranular and fatigue corrosion and especially to the first two types. Intergranular and stress corrosion are very often found in welds.

* Potential signs in this work are those adopted by the Electrochemical Society, Inc. These are opposite to those employed by the American Chemical Society.

2. Stress and Intergranular Corrosion

These two types of corrosion are discussed together as the causes and the mechanism of corrosion in both cases seem to be very similar. In the case of aluminum alloys, stress corrosion is intergranular.[37, 39–43, 50] Both types of corrosion seem to be accompanied by internal stresses caused by the corrosion itself. However, in the case of stress corrosion, additional stresses, produced either by an external load or by shrinkage or by any other cause, are present. The stress-free intergranular corrosion has a rather general character while the stress-corrosion is usually confined to a narrow band of alloy. This band is perpendicular to the direction of stress.[43]

(*a*) *Mechanism of Stress and Intergranular Corrosion.* An aluminum alloy may be the victim of stress or intergranular corrosion if precipitates are present on the alloy grain boundaries. This is because the depleted grain peripheries in a given corrosive medium have different electric potential than the precipitates and grain centers.

Dix and Brown[44] give electrode potential values for some aluminum solid solutions and constituents. When an alloy contains grain boundary precipitates, selective corrosion on the grain boundaries may start. The precipitates under the microscope may look either like continuous envelopes or streaks of globules on the grain boundaries. They also may be present on the slip planes of the grains. However, the discontinuity of precipitates on one microscopic plane does not mean that they do not form a continuous network. This is because the microscopic plane concerned may intersect only some of the branches of the precipitates.

Let us consider an alloy composed of solid solution grains with precipitates

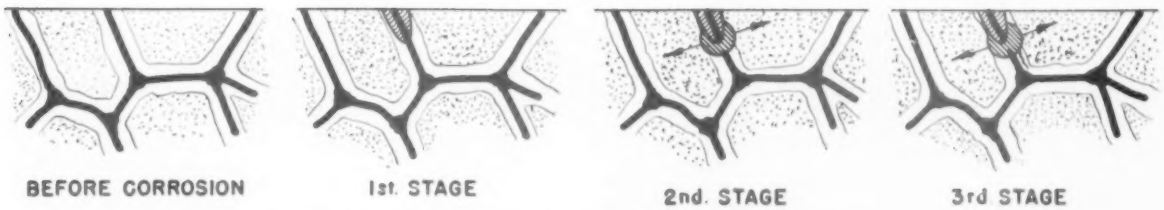

Fig. 30 Schematic representation of intergranular corrosion

forming a continuous network on the grain boundaries. In the case of pure intergranular corrosion the mechanism of the phenomenon seems to be as follows (see Fig. 30):

First Stage. A pit at the grain boundary is formed at the surface due to potential difference between the precipitate, the grain core and the depleted grain periphery.

Second Stage. An oxygen-type concentration cell corrosion may enlarge the pit at its bottom. Corrosion products exercise pressure on the grains (as is known the volume of aluminum corrosion products is much larger than the volume of the aluminum "eaten"). This produces a stress concentration at the bottom of the pit.

Third Stage. A microfissure appears at the bottom of the pit. The fissure destroys any protective film which may be formed.[39] This microfissure, as a capillary, sucks in moisture and, in turn, produces further concentration cell corrosion. This, combined with the potential difference between the precipitates, the grain center and the depleted grain periphery, causes further progress of corrosion. Local strain accelerates the corrosive attack.[39] When there is no continuous network of precipitates, the corrosive action may be substantially slowed or even completely stopped.

In the case of stress corrosion, where the alloy is under stress, in the first stage, the bottom of the pit produces a notch with stress concentration which helps to develop a microcrack. Consequently, stress corrosion will proceed faster than intergranular corrosion and on an accelerated scale, until, finally, the decreased section of remaining uncorroded alloy will fail in a brittle manner.[39]

(b) Types of Stress and Intergranular Corrosion. There seem to be three types of stress or intergranular corrosion in aluminum alloys. This depends primarily on the solution potential of the precipitates.

Type I corrosion occurs when the precipitate is cathodic to the depleted area adjacent to the precipitate. An alloy may be sensitive to surface corrosion when the precipitate is uniformly dispersed, as in the case of solution heat treated and aged alloy. When the precipitate exists in the form of large globules in the grain boundary, as after artificial aging, local pitting

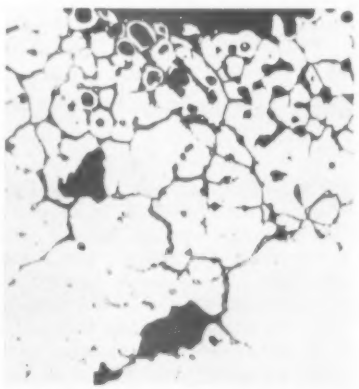

Fig. 31 Intergranular corrosion starting at the bottom of a corrosion pit in the heat-affected zone (24S-T3 alloy sheet, oxy-hydrogen weld, 24S filler metal)

Specimen immersed for 96 hr in 7% NaCl aqueous solution + 3% H_2O_2. Initial structure as in Fig. 29. × 350

may occur. When, however, the precipitates form a continuous network (Fig. 29) around the grains, intergranular or stress corrosion usually occurs (Fig. 31). Aluminum-copper alloys are representative of this type of corrosion. In this case, copper aluminide ($CuAl_2$) intermetallic compound is precipitated. It is cathodic to the depleted grain periphery and the grain periphery is also anodic to the grain core.

Type II intergranular or stress corrosion occurs when the precipitate is anodic to the depleted grain boundary and to the grain core. Aluminum-magnesium alloys belong to this group. The β-phase, being anodic, corrodes.[29, 35, 38, 39–42, 45] In Al-Mg alloys (similar to Type I), intergranular attack happens when there exists a continuous β-phase intergranular network or when there are small size globular β-phase precipitates. Large size precipitates, called "string of pearls"[35, 45] or "lakes,"[40] produce only local pitting.

Lacombe and Chaudron[35, 36] found that up to 280° C a metastable β'-phase precipitates and above this temperature β' transforms into the stable β-phase. The former is less resistant to corrosion than the latter. Minimum resistance to stress corrosion has been found after low-temperature aging (100–150° C).[38]

Al – 5% Mg is practically resistant to intergranular or stress corrosion after solution heat treatment.[29] However, β (or β') phase may slowly precipitate even at room temperature in a few years and even in a few months under tropical conditions.[29]

In Germany, where Al-Mg alloys were originally developed, alloys containing 5% or more magnesium were discontinued and replaced by Al-Mg-Si alloys. The latter ones have similar or better properties than Al-Mg alloys but are less troublesome with respect to intergranular corrosion.[11, 46]

Type III stress or intergranular corrosion occurs when the intermetallic precipitate is alternatively cathodic or anodic, depending on environment, or whether the constituents are filmed or not. Aluminum-zinc alloys are subjected to this type of corrosion. In most neutral or acid solutions, zinc is anodic to aluminum or aluminum-rich solution, but in alkaline solutions the potentials are reversed.[41] A zinc-rich, solid solution is precipitated on the grain boundaries in Al-Zn alloys and not intermetallic compound as in Type I and Type II corrosion.

In normal environment, the precipitates, the depleted grain boundaries and the grain cores are filmed.[37] When, however, a crack is produced, this film is temporarily destroyed and the order of potentials reversed (Fig. 32). Consequently, the zinc-rich precipitate and depleted grain periphery alternatively corrode, depending on whether the film is present or destroyed.

3. Corrosion Fatigue

Corrosion fatigue fractures are predominantly transcrystalline and they follow slip planes which are formed during cold working of the alloy.[51] Corrosion-fatigue usually starts at the bottom of corrosive pits which form stress concentration under cyclic loading. After corrosion fatigue has been started it progresses further as pure fatigue.[51]

4. Effect of Cold Working

Cold working, besides affecting the potential of the alloy as a whole, increases the rate of precipitation on the grain boundaries,[35, 40, 48] and along the slip planes. Consequently, cold working strongly affects resistance to corrosion. The rate of corrosion, in the case of stress or intergranular corrosion seems to increase with the increase of the percentage of cold working and, after reaching some maximum at

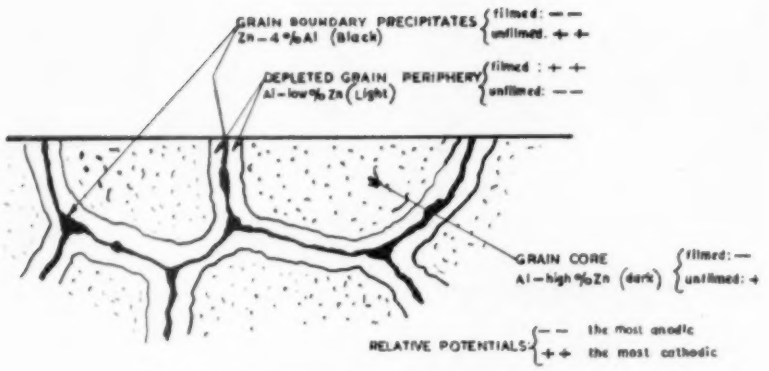

Fig. 32 — Aluminum-zinc intergranular corrosion

a particular precentage of cold working, seems to decrease.[40] According to Zeerlender[48] in Peraluman 5 (Al – 5% Mg) the rate of corrosion increases up to about 22% of cold working and then decreases as the cold working increases above this limit.

A very small amount of cold working however, such as used for straightening the sheet, may cause an enormous grain growth* during recrystallization.[28, 52] Such grain growth may also produce a heavy precipitation and, besides increasing cracking, may increase the sensitivity to corrosion, especially to intergranular attack.

5. *Prevention of Corrosion*

(*a*) *Solution Heat Treatment.* The best remedy against corrosion is homogenization of the alloy in order to decrease the formation of local galvanic cells. This may be achieved by solution heat treatment during which the precipitates are dissolved and kept in a supersaturated solid solution due to rapid cooling. This, however, is effective only for alloys containing a low percentage of elements which precipitate such as copper (e.g., in 248) or magnesium (up to 5%).[20, 25, 46] In alloys with higher percentages of these elements it is impossible to keep them completely in solid solution; some amount will precipitate. Fundamentally, aluminum-magnesium alloys are age-hardening alloys.[20] As has been stated above, the β phase may precipitate in hot tropical climate and even in normal room temperature. George and Chalmers[49] state that the β phase precipitates on the grain boundaries even in Al – 5% Mg alloy. They, however, refer to cold-headed rivets, consequently to a cold-worked alloy.

(*b*) *Artificial Aging.* During artificial aging, when the viscosity of the alloy is decreased and surface tension of the precipitates is high, the latter form small spheroids on the grain boundaries and on the slip planes. For this reason the continuous network of precipitates on the grain boundaries is broken and sensitivity to intergranular corrosion is decreased. This treatment, however, is much more effective in Al-Mg alloys, where the β-phase precipitate is anodic to the solid solution grain, than in Al-Cu alloys in which the $CuAl_2$ precipitate is cathodic. In the latter case during corrosion $CuAl_2$ is not consumed; it stays in the pits and may cause the deepening of these pits.

The size of the β-phase precipitate on the grain boundaries in Al-Mg alloys is critical.[36] Too fine but closely spaced globules produce intergranular attack. Zeerlender[45, 46] and Metcalfe[35] call the large β-phase precipitates "strings of pearls" and the treatment which produces them, "heterogenization." This treatment consists of solution heat treatment and very slow cooling at a rate of 10[45] to 50° C/min.[36] Brenner and Roth[36] state that heterogenization may be achieved by heating the alloy at a temperature just below the solid solubility line.

(*c*) *Addition of Alloying Elements.* Zinc decreases the rate of precipitation of the β phase in Al-Mg alloys but, once a continuous network is formed, zinc has no effect on stress-corrosion.[40] Also the addition of manganese and chromium decreases the intergranular corrosion sensitivity of these alloys.[75]

6. *Significance for Welding*

Homogeneity of an alloy is one of primary conditions of good resistance to corrosion. Unfortunately, welding is a process which *produces heterogeneity.* First of all, there almost always exists some difference in chemical composition between the weld and the base metal. Secondly, the weld metal is a cast alloy, while the base metal may be wrought or cast. Besides, the base metal, due to cold working, may contain locked-in stresses, and the whole welded joint may be under shrinkage stresses. On the other hand, the weld metal and the heat-affected zone of the base metal may also be structurally heterogeneous. The lack of homogeneity of the former may be due to solidification (coring) and of the latter, due to the secondary heat effect. Furthermore, the heat-affected zone during welding undergoes structural changes like annealing, hardening, recrystallization, grain coarsening, diffusion, precipitation, etc. All this may produce local electric cells and is a potential source of corrosion.

The heat-affected zone is the first to suffer from corrosive attack because of previously described reasons: grain boundary precipitates, grain growth, blistering and porosity. A fine crack or fine open porosity may act as a capillary, thus may suck in moisture and produce concentration cell corrosion forming deep pits as a result (Figs. 31, 37 and 38).

The melting point of a welding flux is lower than that of welded alloy. Consequently, when cracks or blisters are formed above or just below the solidus, when the flux is still fluid, the depression produced during cooling inside a crack or a blister may suck the fluid flux in. This flux usually cannot be removed by any physical or chemical means. In this case the alloy may experience a selective concentration cell corrosion attack in the cracked or blistered area. This, as a rule, will be accompanied by an intergranular (Fig. 31) or stress corrosion attack as cracks and blisters usually occur in the area which shows precipitation on the grain boundaries.

Such a phenomenon was observed on welded Hy-5 alloy.[52] This alloy possessed an extraordinary resistance to corrosion when *unwelded.* Immersion for 1400 hr in a 10% aqueous solution of NaCl + 3% H_2O_2, or in salt spray would show no corrosion. However, oxy-acetylene gas-welded and severely cracked specimens have been severely attacked in a few days, when exposed to a humid atmosphere. The attack was intergranular and was especially severe around the crack. Those welds did not produce any visible porosity.

Cracks and lack of tightness have sometimes been observed on gas-welded parts stored for some time after welding,[15, 52] although visual or fluorescent penetrant inspection has not shown any such defects immediately after welding.[52] It is suspected that cracking may have been produced due to the pressure exerted by corrosion products inside attacked blistered areas.

The effect of precipitates could be counteracted by a homogenizing (in, e.g., Al-Cu alloys) or by heterogenizing (in, e.g., Al-Mg alloys) provided the weld is neither cracked nor blistered nor porous. This, however, in the majority of cases would be impractical. Aluminum alloys have too low strength at the heat treatment temperature and, consequently, parts would require intricate jigging; otherwise, they might collapse. On the other hand, in the case of Al-Cu alloys (like Duralumin) the $CuAl_2$ precipitate is very slow in going into solution. The author tried before the war to decrease the

* (see "Grain Growth and Recrystallization" above).

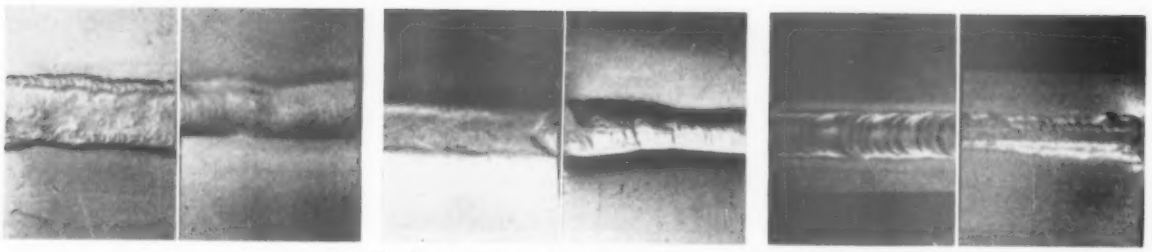

Fig. 33 528-O alloy welded sheet before corrosion test

(*Left*) Oxy-hydrogen weld. (*Middle*) Oxy-acetylene weld. (*Right*) Inert gas (argon) weld. 528 filler metal. NOTE: In each pair of specimens the left-hand side shows the face side and the right-hand side shows the root side of the weld.

corrosiveness of welded 2-mm Duraluminum sheets.[32] Only after 1 hr of soaking at 490±10° C could some sign of a partial dissolution of the $CuAl_2$ precipitate be noted, manifesting itself by breaking of the continuous precipitate envelope around the grains. There was also an interesting shifting of corrosive attack from one area of the specimens to another, depending on the length of soaking time.

However, there is no other means of preventing corrosion in a blistered or cracked area than by drying the part completely and keeping it dry during service. This, of course, would very seldom be possible or practicable.

7. *Selected Example*

A few corrosion tests have been run in order to see what would be the result of welding on resistance to corrosion: Three series of three specimens from 0.080 in 528-O alloy have been prepared by oxy-hydrogen, oxy-acetylene and inert-gas (argon) welding. The used material showed low porosity. All specimens were made from the same sheet, by the same welder and under identical conditions. Figure 33 shows these specimens welded, but before the corrosion test. Oxy-hydrogen specimens show the widest weld among the three series.

In Fig. 34 sections of these specimens (Fig. 33) are presented. On the oxy-hydrogen welded specimen porosity and blistering may be seen but only on the face side. Oxy-acetylene welded specimens do not show any porosity, however, a light swelling may be seen. This would suggest that in these specimens some microporosity is present. Inert-gas-welded

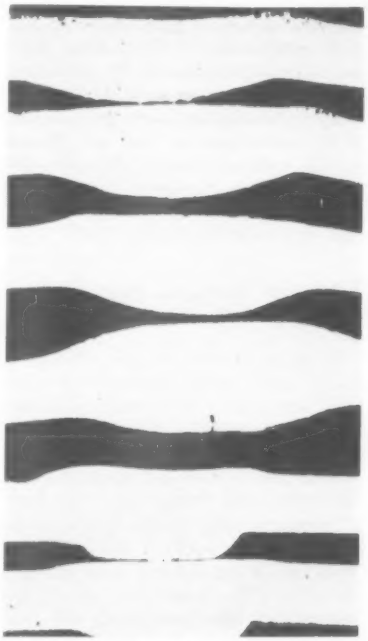

Fig. 34 Sectioned specimens from Fig. 33. × 5

(*Upper*) Oxy-hydrogen weld. (*Middle*) Oxy-acetylene weld. (*Lower*) Inert-gas (argon) weld.

specimens appear to be porosity free under low magnification. However, higher magnification (Fig. 35) revealed some small pinholes located rather toward the center of the specimens and toward the weld metal.

Figure 36 shows the specimens from Fig. 33 after 168 hr immersion in a 7% aqueous solution of NaCl + 3% H_2O_2:

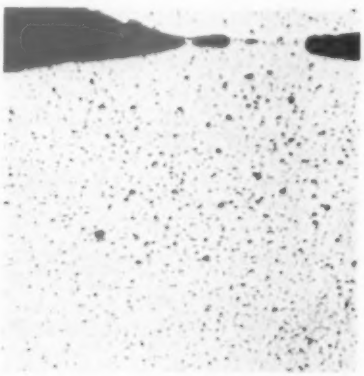

Fig. 35 Heat-affected zone and weld metal microporosity in an inert-gas-welded specimen (see Figs. 33 (right) and 34 (lower)). E, × 50

Figure 37 after the corrosion products have been cleaned off and Fig. 38 gives the section of the corroded specimens after 312 hr immersion in the electrolyte.

Results are given in the following paragraphs.

1. Oxy-hydrogen Welded Specimens: Heavy pitting in the heat-affected zone close to the weld metal (on the face side only). Heavy pitting in the heat-affected zone some distance from the weld on the root side. Scattered pitting in the base metal outside the heat-affected zone. Some pitting in the weld metal.

2. Oxy-acetylene Welded Specimens: Heavy uniform pitting throughout all specimens on both sides. Somewhat lesser pitting in the heat-affected zone.

3. Inert-Gas-Welded Specimens: Base and weld metals light scattered pits. Heat-affected zone close to the weld me-

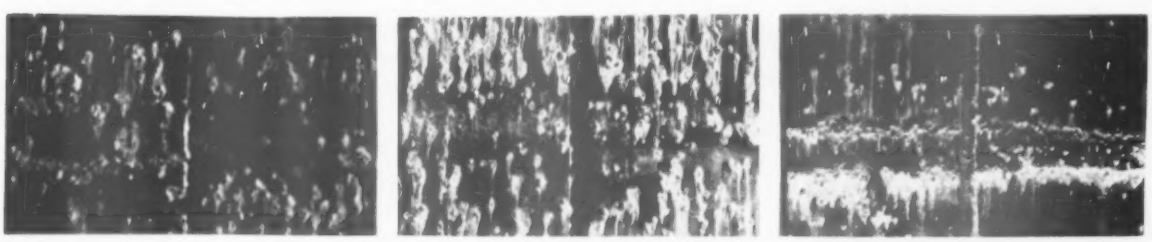

Fig. 36 Same as in Fig. 33 after 168 hr immersion in 7% aqueous solution of NaCl + 3% H_2O_2 (solution changed every day)

Fig. 37 Same as in Fig. 36 after corrosion products have been cleaned off. Note black deposit of MgO

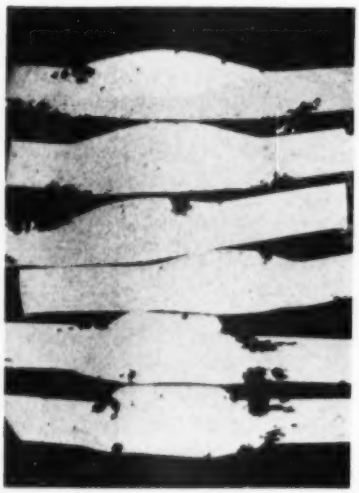

Fig. 38 Sectioned specimens from Fig. 37 after 312 hr. immersion in the electrolyte. × *3*

(*Upper*) Oxy-hydrogen weld. (*Middle*) Oxy-acetylene weld. (*Lower*) Inert-gas (argon) weld. Face side of welds points downward.

tal—very heavy continuous corrosive attack.

All specimens show heavy blackening of the immersed areas. This suggests the formation of MgO on the surface. Also some intergranular attack around pits is present.

As may be seen, the oxy-acetylene welded specimens show the best over-all resistance to corrosion and the inert gas welded, the worst. The observation of Figs. 6 and 33 through 38 seems to indicate that there is some interdependence between blistering and porosity on the one side and corrosion on the other. It also seems that the flux used for gas welding does give some protection against gas absorption (Note that the inert-gas specimens are more heavily attacked on the root than on the face side. The root side does not have the inert gas protection).

Other corrosion tests with 24S-T3 alloy welded specimens showed very similar over-all trend. The oxy-acetylene welded specimens were corroded rather slightly; oxy-hydrogen welded, heavily; and inert-gas welded, very heavily. It is felt, as previously stated, that any explanation of this phenomenon would at present be pure speculation. Therefore, it would be better to refrain from it.

VII. STRENGTH AND PHYSICAL PROPERTIES

Besides resistance to corrosion already dealt with, welding also affects the physical and strength properties of the welded alloy. Such physical properties as heat, sound and electric conductivity, coefficient of thermal expansion, etc., are affected by the welding process. However, discussion of these changes is not of a particular interest. Consequently, let us rather consider strength properties.

1. Tensile Strength, Yield Strength and Ductility

There are three distinctive zones on a weld: weld metal, heat-affected zone and base metal. Weld metal is a cast metal; base metal may be cast, forged, hot or cold rolled, etc.; and the heat-affected zone represents base metal which underwent some changes due to the heat.

Consideration of the elongation of the weld as a whole would be illogical as the weld joint is composed of three different materials. The weakest one, usually the heat-affected zone base metal, will elongate the most and will have a tendency to take all the strain.[15] It would also be useless to use a filler metal which gives too strong a weld metal because the strain on the heat-affected zone would be still higher. Too weak a weld metal would produce high strain in itself. These two zones are always to be critically considered because they take all the strain and because the weld usually fails there.

There is no data (at least the author is not aware of the existence of any) showing the effect of blistering, porosity or cracking on tensile strength, yield strength and ductility. There exists, however, some data concerning the weld efficiency (the percentage of the tensile strength of the weld to that of the base metal).[13, 14, 31] In Al – 5% Mg the joint efficiency may be as high as 100%. This joint efficiency, of course, concerns relatively sound welds.

Heat treatment may considerably increase the joint efficiency of welds made with heat-treatable alloys. Martin, Jacobson and Voldrich[14] give some figures concerning the joint efficiency of welded thick plates. According to their report, the joint efficiency increases from 70–80% to 100% for 61S, from 60–70% to 80–90% for 24S and from 50–60% to 50–60% for 75S after heat treatment.

2. Dynamic Properties (Fatigue Strength)

The investigation of fatigue properties of the weld is an extremely delicate proposition. This is primarily due to the large number of variables which may affect fatigue joint efficiency. There are four groups of such variables which are due to (*a*) material (base and filler metal), (*b*) method of welding, (*c*) geometry of the weld and (*d*) method of testing. Some excellent works have been published in recent years which dealt with the fatigue strength of a welded joint of aluminum alloys.[54, 55]

These two papers confirm the general trend of results in welded steel joint found by other research workers like Schultz and Buchholtz [56, 57] Schick[57] and by the Royal Aircraft Establishment, Farnborough, Hants, England. They should be read and studied by everyone concerned with the design of welded joints.

However, there are still no reliable fatigue data on welded joints which could be directly used for design purposes. It also seems that designers will have to wait still many more years before such data are available.

VIII. GENERAL CONCLUSIONS

A review of the international literature which deals with the weldability of aluminum alloys seems to indicate that:

1. The weldability of aluminum alloys is still far from being sufficiently explored.

2. There is a definite need for aluminum alloys which would possess: (*a*) high strength (static and dynamic), (*b*) reasonable ductility, (*c*) good resistance to corrosion and (*d*) good weldability.

3. It would be unadvisable to seek a universal weldable aluminum alloy for thin and thick sections and resistant to all corrosive conditions.

4. A good weldable alloy during or after welding should be: (*a*) porosity free, (*b*) insensitive to cracking, (*c*) insensitive to corrosion, (*d*) insensitive to fatigue and (*e*) rather of a relatively stable solution type. Also aging properties at higher temperature (above 300° F) would be desirable.

Appendix I

1. ETCHING SOLUTIONS

Solution A

Sodium hydroxide ($NaOH$)	5 g
Water (H_2O)	95 cm³

Solution B

Nitric acid (HNO_3) conc.	50 cm³
Water (H_2O)	50 cm³

Solution C (Tucker's Etchant)

Hydrochloric acid (HCl) conc.	45 ml
Nitric acid (HNO_3) conc.	15 ml
Hydrofluoric acid (HF) conc.	15 ml
Water (H_2O)	25 ml

Solution D

Sodium Hydroxide ($NaOH$)	1 g
Water (H_2O)	99 cm³

Solution E (Wasserman's Etchant)

Solution (*a*) 0.5% hydrofluoric acid (HF)		
Solution (*b*)	Hydrochloric acid (HCl)	2 cm³
	Nitric acid (HNO_3)	20 cm³
	10% Potassium dichromate ($K_2Cr_2O_7$)	50 cm³

Mix 1 volume of (*a*) with 1 volume of (*b*).

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2. CHEMICAL COMPOSITION OF DISCUSSED ALLOYS, %

Alloy	*Al*	*Cu*	*Fe*	*Si*	*Mn*	*Mg*	*Zn*	*Cr*	*Ni*	*Ti*	*Other — Each*	*Other — total*
248*	Remainder	3.8–4.9	0.50	0.50	0.3–0.9	1.2–1.8	0.10	0.10			0.05	0.15
Clad 248 (Core)*,†	Remainder	3.8–4.9	0.50	0.50	0.3–0.9	1.2–1.8	0.10	0.10			0.05	0.15
438*	Remainder	0.30	0.80	4.5–6.0	0.05	0.05	0.10			0.20	0.05	0.15
528*	Remainder	0.10	‡	‡	0.10	2.2–2.8	0.10	0.15–0.35			0.05	0.15
618*	Remainder	0.15–0.40	0.70	0.4–0.8	0.15	0.8–1.2	0.20	0.15–0.35		0.15	0.05	0.15
Hy-5§	Remainder				0.30	5						
Hy-7§	Remainder				0.30	7						
Duralumin§	Remainder	4.5		0.3	0.5	0.5						

* Alcoa Aluminum and its Alloys, 1950.
† Coated with 99.3% min Al.
‡ Fe + Si = 0.45% max.
§ Nominal composition. O = Annealed. T3 = Solution heat treated, cold worked and aged. T4 = Solution heat treated and aged. T6 = Solution heat treated and artificially aged.

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Recent Developments in Powder Processes

Powder cutting was originally developed as a method of cutting stainless steel. Recent developments extend its use to scarfing of blooms and billets and powder washing of steel castings and the cutting of nonferrous metals and firebrick

by R. S. Babcock

POWDER CUTTING was conceived and developed as a method of progressively cutting stainless steel. Stainless steels normally resist oxygen cutting because of the refractory nature of the chromium oxide layer which forms immediately when stainless steel is heated in the presence of oxygen. The addition of iron powder to such a reaction prevents the solidification of refractory oxides through the generation of additional heat and a fluxing action of iron oxide on chromium oxide.

Initially, powder-cutting apparatus was designed so that the powder was introduced into the oxygen stream at some point within the nozzle. Similar arrangements were used in larger nozzles for powder scarfing, a method of conditioning or removing defects from the surfaces of stainless-steel slabs and blooms. The powder processes were so much faster and more efficient than the existing "wiggle" techniques and carbon arc methods of cutting stainless steel that there was an immediate demand for apparatus in many commercial applications. In early stages of development it was found that improved performance could be obtained by discharging the powder through an annular passage between the preheat ports and oxygen bore of the cutting nozzle. This permitted the use of higher oxygen pressures, independent of dispenser pressures, and provided for complete penetration through much heavier sections of stainless steel.

However, it was necessary to operate all of these cutting and scarfing nozzles well above the work surface because only part of the powder was brought into direct contact with the preheat flames, and the remainder of the powder traveled some distance before reaching ignition temperature. In powder-scarfing operations, the nozzle was carried at least 3 in. and sometimes 4 in. away from the reaction zone. This appeared to be an undesirable condition when compared to cutting and scarfing operations on carbon steel. Maximum operating speeds and rates of metal removal are normally obtained with minimum spacing of the nozzle above the workpiece, or with the flames touching the workpiece surface. The development of the present method of introducing powder externally made it possible to operate powder nozzles closer to the work surface.

In external powder feed, the air-conveyed powder is directed through the preheat flames where it is raised to ignition temperature and enters the oxygen stream at a point close to the tip of the nozzle. Since all of the heated powder particles are ready to start burning as soon as they reach the oxygen stream, the nozzle can be

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Fig. 1 Manual powder cutting, used to trim stainless steel in a refinery application

located closer to the work surface. This results in faster cutting speeds, reduction in the amount of powder required and a lower rate of conveying air flow. The latter is an important factor, because the nitrogen of the conveying air tends to dilute the purity of the cutting oxygen stream.

However, in scarfing, it was found that the best rolling results were obtained from the smoothest scarfing passes, and internal powder feed produced the smoothest scarfing pass. The powder-scarfing of blooms and billets was satisfactory from the standpoint of cost and rolling results, but occasionally a condition referred to as "shiners" was found on the surface of stainless steel sheet rolled from powder-scarfed slabs. Metallurgical analysis pointed to a high ferritic concentration at the location of these shiners. This condition apparently was due to partially burned powder being deposited at the trailing edge of the reaction zone where it was subsequently rolled into the top surface of the slab unless removed with the furnace scale. Field trials of powder scarfing with external powder feed proved that this new method was not only faster and more economical, but was also less likely to leave unburned powder on the scarfed surface. External powder feed scarfing passes could be maintained at speeds as much as three times faster than those obtained with internal powder feed. External powder feed scarfing is now being used exclusively in conditioning stainless steel slabs.

The change to external powder feed made it possible to use large slotted nozzles which increased surface coverage per pass. Although the scarfed surface produced with this equipment was considerably rougher than the product of the earlier apparatus, it was found that this surface rolled out to the complete satisfaction of all concerned, and there has been no recurrence of the "shiner" trouble. Producers of stainless steel are currently conditioning up to 50% of their stainless steel slab production using external powder feed equipment and at a saving of more than $5.00 per ton compared to

Fig. 2 Manual powder cutting being used to remove a section from a cast-iron header

Fig. 3 Powder cut through 20-in. thick cast-iron flywheel rim

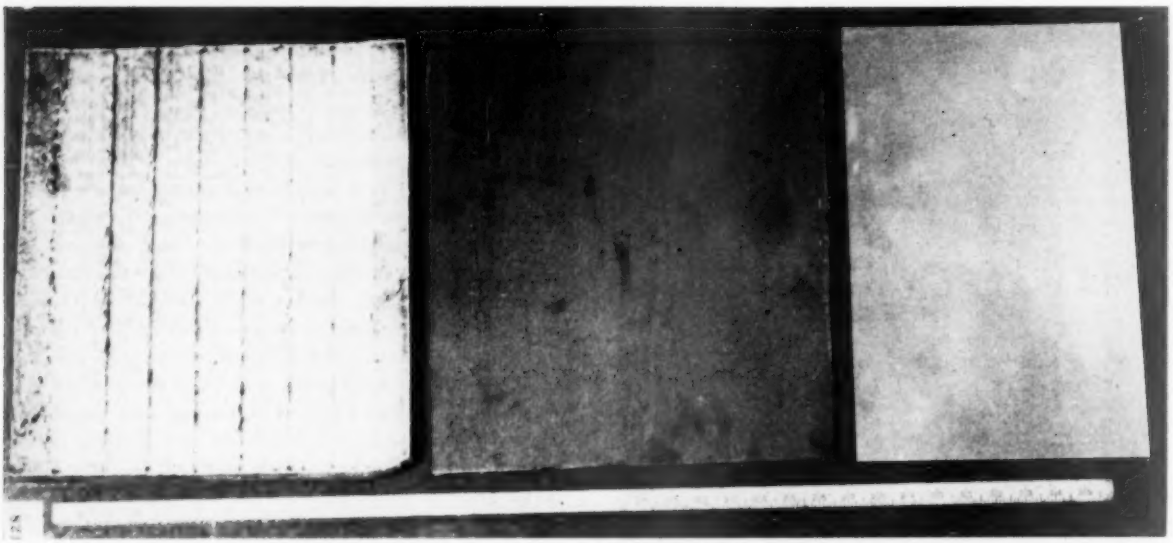

Fig. 4 Three sections of stainless steel representing material as powder scarfed, hot rolled to 5/16 in. thickness and finally rolled to 0.100 in. thickness

grinding. The mechanization of the powder-scarfing process has led to greater efficiency through the use of larger powder-scarfing nozzles. Whereas a 2-in. wide pass is produced by the manually operated equipment, it is now possible to make a 4-in. wide pass with a larger machine-borne powder-scarfing nozzle.

Powder washing is another powder process that has shown tremendous growth since the early days of internal powder feed. Powder washing has proved to be an effective means of removing sand incrustations and penetrations from steel castings. For many years the steel foundries have been faced with a mounting shortage of chippers and apprentice chippers. In the search for improved methods of metal removal, steel foundries found some encouragement in the oxygen-trimming, scarfing and gouging processes. There were serious limitations to these processes in that the reactions must be carried forward progressively, and the surfaces produced were undesirably grooved with ridges at the intersection between passes. The development of a washing operation, which permitted sideward movement over a wide area, led to the design of the present washing equipment. This high preheat capacity apparatus has an in-line head with a gouging type of nozzle equipped with preheat flames completely encircling the oxygen orifice. The addition of an external powder feed served to further stabilize the washing reaction and to increase the rate of metal removal to 2 lb or more per minute.

The first demonstration of powder washing included the removal of a penetration from a keyway. A penetration results from a breakdown of the sand mold in which the molten steel penetrates between the grains of sand in the mold and solidifies so that after the mold is removed this sand is held in a matrix of steel as a lump on the outside surface of the casting. This keyway which was 2 in. deep, 6 in. wide and 16 in. long, was completely filled with such a penetration. After approximately 1 min of application, the reaction had been carried down through the sand and steel to the bottom of the keyway where the casting surface appeared as a darker surface in the reaction zone. At this point, the foreman who was watching the process very closely, personally took the blowpipe from the operator and washed out the keyway completely in about 15 min. Since the removal of such a penetration usually requires several hours of chipping, it is easy to understand the customer's enthusiasm for this process. Since then, many successful demonstrations have been made in other steel foundries which led to a very rapid

Fig. 5 Mechanized powder-scarfing stainless steel slab surface

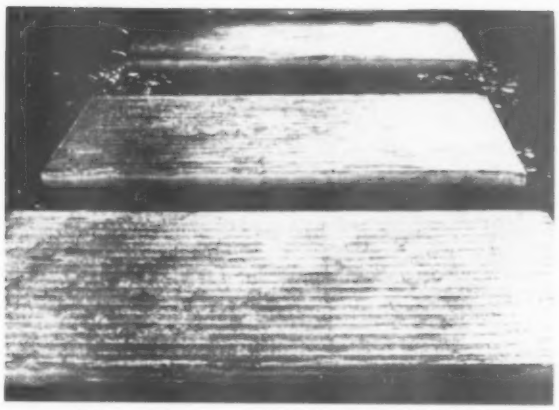

Fig. 6 Powder-scarfed stainless steel slabs

expansion of the new powder-washing process. This process has also spread to other countries and is currently being used in steel foundries all over the world.

Although the powder-cutting process was originally developed for cutting stainless steel, it was noted very early in the development stage that the process could be used in the cutting of copper, nickel, Hastelloy alloy, brass and even firebrick. Reinforced concrete has also been severed by the powder process. In the removal of some concrete piers approximately 14 by 30 in., it was found that each required several hours of pounding with a heavy jackhammer. The powder-cutting equipment was mounted on a machine in a position to make horizontal cuts and it was found that a 14-in. thick section could be penetrated at a progressive speed of $1^1/_2$ ipm, so that only 20 min was required for complete severing of each section. The action of the powder-cutting process in this case is primarily one of controlled melting with the source of heat supplied by the combustible powder. Higher temperatures were obtained by adding aluminum to the iron powder, and a single-tube attachment permitted the discharge of higher powder flows and minimized the tendency toward clogging or sintering.

Several other noncombustible materials have been cut and severed by the powder-cutting process, including the scrapping of bronze ship propeller blades. In the production of war material, it is necessary to plan for an adequate quantity of repair parts. Based on the probable life of a given style of vehicle, tank, plane or ship, a suitable quantity of spare parts is authorized for each unit. As units are obsoleted by redesign for more efficient operation or are placed in "moth balls," as in the case of surplus ships, fewer supplies are required and the surplus quantity of propellers, etc., was marked for scrapping. The demands for material to make new propellers of different pitch or size, or blade shape, caused many material expediters to seek an economical means of reducing the large manganese-bronze propellers in the scrap piles. In order to remelt large three-bladed ship propellers, it is necessary to remove two blades adjacent to the hub to meet the size limitation of the remelt furnace openings. The propeller blades vary in size, ranging up to 6 or 8 in. maximum thickness and and 30 to 40 in. wide along the line of cut. Various methods were used including chipping, drilling and blasting, and are melting, but in all cases the severing time was excessive. Powder cutting was introduced and it was demonstrated that these sections could be cut by machine at a great saving in labor and material.

There have been similar jobs of reducing scrap manganese-bronze flywheels. A 4-in. thick wheel required two days operating time and 10 power saw blades to reduce the material to furnace charging size. A 3-ton casting containing $2000 worth of metal, 9 to 11 in.

Fig. 7 Riser stub on steel casting before washing

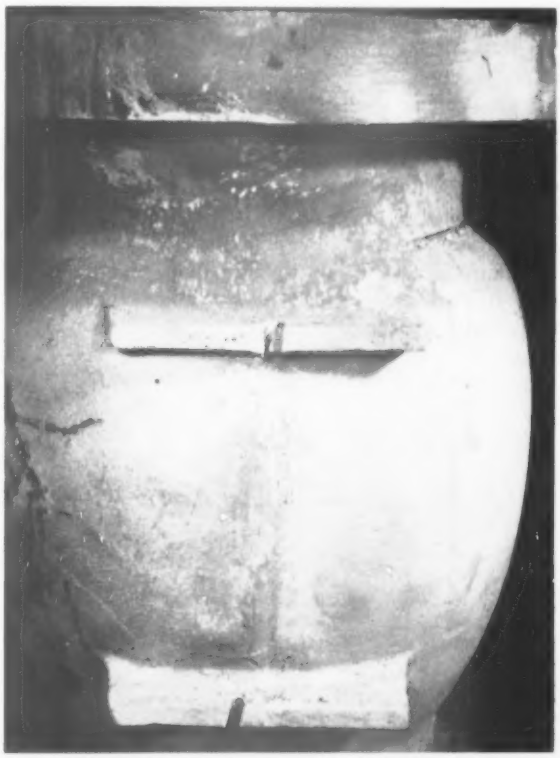

Fig. 8 Casting shown in Fig. 7 after washing

Fig. 9 Powder cutting reinforced concrete

thick, can be powder cut in a fraction of this time, and requires only $130 worth of labor, gas and powder in the manual reduction of the material to charging size. This operation has now been mechanized and requires only $200 worth of consumable materials and labor to reduce seven tons of manganese bronze.

Inconel, Monel and nickel slabs up to 6 in. thick are being cut at a minimum speed of 4 ipm, using aluminum and iron powder mixtures, single-tube attachments and standard cutting blowpipes on straight-line cutting machines.

The ability to obtain flying starts with the powder-cutting and scarfing processes has led to the use of powder starting in production cutting of carbon steel. The heat transferred from the burning powder particles makes it possible to eliminate the preheating period normally required to raise the starting edge to ignition temperature. Powder starting is now used in mechanized cutoff operations on rounds and round-corner square bars where previously the preheating period amounted to between 15 and 25% of the cutting cycle. For these operations a powder attachment is added to the billet cutting setup and the powder flow is limited to a very short period, timed to coincide with movement of the nozzle over the starting edge of the billet. The powder flow is shut off for the remainder of the cutting cycle. This use of powder starting means more production per unit of labor and a lower cost per cut and also consistency in starting with multiple blowpipes. Powder starting has been applied to multiple-blowpipe installations in slicing 13- to 28-in. diam tapered ingots with 6 cutting blowpipes, and even to 10-blowpipe shape-cutting machines operating on carbon steel plate.

Fig. 10 Cutting skull in line with track support

One of the major uses of powder cutting today is in the reduction of skulls, spills and salamanders to furnace charging sizes. The continued high rate of steel production has placed a premium on heavy melting scrap. Until recent years, low yield or difficult-to-cut sections were dragged out to the slag dump and buried. The only tool then available for the reduction of these sections was the oxygen lance. The cost of lancing—including pipe, oxygen and labor—frequently exceeded the value of the material being reduced. Heavy cutting blowpipes capable of severing sections up to 6 ft thick were combined with suitable cutting machines and rigging to form scrap reduction units. These installations have met the demand for faster, safer and more economical methods of preparing heavy melting scrap. A high proportion of this material has surface or subsurface slag which interferes with progressive cutting. Iron powder was introduced as a means of overcoming these oxidation-resistant materials. However, the most effective method of overcoming external slag was found to be the use of a mixture of aluminum and iron powder.

Fig. 11 Powder lancing through concrete

Experience has also shown that higher production in these heavy scrap-cutting operations can be achieved by providing a sturdy cutting setup. There have been almost as many different heavy cutting set-up designs as there are installations since each reflects the individual requirements of location, type of material, handling facilities, furnace charging size limitations and design interpretations by local personnel. The most popular skull-cutting rigging consists of a heavy-duty cutting machine carriage with a 6- to 8-ft long arm pivoted on a turret base directly over the center of the carriage. The heavy cutting blowpipe at the end of the arm is counterbalanced by a weight at the other end so that it can be rapidly positioned at the desired line of cut over skull sections. Track supports have been made in

many forms, ranging from fixed structural frames to gantry or bridge units.

Another method of mounting the track involves a jib boom support hinged from a center post. Variations of this have been made with a side rail or wall-type crane for supporting the operating platform. A recent improvement used on several of these installations is a turntable mounting for the cutting machine track. This enables the operator to make successive cuts in different planes so as to reduce the skull to charging size without repositioning the material.

Cost studies of skull-cutting operations in several steel mills have shown that the average cost of reducing heavy melting scrap to furnace charging size ranges from $2.50 to $5.00 per ton.

The maximum section that can be oxygen cut depends upon uniformity of the material and its composition. A cast-iron press base 48 in. thick, 8 ft wide and 12 ft long was cut up into four sections, using a C-45 blowpipe and powder attachment. Armor steel ingots weighing 538,000 lb, having a section 60 to 66 in. thick by 10 ft wide, were reduced to forging size with a C-45 blowpipe. The thickest section that has been cut, according to available records, was an extremely large ladle button that was 96 in. thick at the maximum point. In cutting this material it was necessary to use a lance in order to clear the bottom of the kerf as the cut progressed.

The powder lance is one of the most recent powder process developments. It is a combination of the deep penetrating oxygen lance with the dependable reaction-supporting properties of the powder processes. The air-conveyed powder is added to the lance oxygen at the lance pipe holder and both flows are controlled by shut-off valves operated by a single lever.

The powder lance is primarily a piercing tool although it can also be used to produce a slot through various materials. It has been found that powder lance holes can be pierced through concrete, cinder block, cast iron, converter kidney sections, slag, aluminum and copper.

The piercing speed depends upon the size of pipe, oxygen pressure and type of powder. Mixtures of aluminum and iron give the fastest operation. The list of accomplishments attributed to the powder lance includes:

Tapping packed cinder notches in blast furnaces following the prolonged 1952 steel strike. Many furnaces were returned to operation by approximately 30 min powder lancing as compared to 10- or 12-hr drilling and piercing with a straight oxygen lance.

Concrete and brick walls, 3 ft thick, were powder lanced through in 10 min using 3 lengths of $\frac{1}{2}$-in. pipe, as compared to 23 min and 10 lengths of $\frac{1}{2}$-in. pipe for straight oxygen lancing.

The salamander was tapped from a blast furnace by piercing a 4-in. diam hole through 15 ft of concrete, refractory, slag and cinder in 26 min. Approximately 325 tons of molten pig iron and slag were drained from the powder-lance hole. The tapping operation consumed 770 ft of lance pipe and 100 lb of powder.

Another salamander was tapped at about the same time in another section of the country. In this case 250 tons of molten material were drained off through a hole pierced with 800 ft of $\frac{1}{2}$-in. lance pipe and requiring 2 hr 15 min lancing time. No powder was used in this case.

SUMMARY

In view of the progress which has been made with the powder-cutting process since its conception, it is logical to expect that there will be even greater advancements made through the development of new applications and improvements of older and well-established applications. The powder-cutting process will continue to grow according to the growth of the stainless steel production in this country, and also as it plays a part in providing material required in the manufacture of ferrous and nonferrous materials. During the next 5 years, it is anticipated that the powder processes will double in the extent of application and scope of operations.

Quarry Maintenance

A paper on quarry maintenance covering a variety of jobs and maintenance problems

by J. Dean Davidson

THE Davidson Granite Co. produces both building and crushed stone. Therefore, our operations embrace those problems common to both the crushed stone and the dimension stone industry. As would be expected, the type of operations and processing equipment are not all similar. Crushed stone production requires the maintenance of electric and Diesel shovels, crushers and screening equipment, while the dimension stone work requires that gang saws and other shaping and finishing equipment be kept in first-class operating condition.

In outlining and summarizing some of our more common oxy-acetylene maintenance jobs, we find that most of them can be classified under the following three headings:

Heavy heating with the oxy-acetylene flame, oxy-acetylene welding and those jobs in which the oxygen-cutting processes are involved. In the illustrations to follow we have grouped the jobs illustrated under these classifications. The first grouping to be illustrated is heavy heating.

Figure 1 is a general view of one of our Northwest Type 80D, 2 1/2-yd capacity shovels used on the floor of the quarry for loading. These shovels are subjected to very heavy usage, particularly in sweeping the quarry floor at the edge of the muck pile. In this sweeping operation there is tremendous lateral stress on the two dipper sticks that can be seen here supporting the dipper. As a result, these dipper sticks become bent, in spite of their heavy construction. Through the use of oxy-acetylene heating or more properly "flame shrinking," these bent "sticks" are straightened right in place with a minimum of lost shovel time.

Before starting of the operation in straightening the dipper stick the operator stretches a line to determine the extent and point at which the sticks are bent. Bow in these dipper sticks results in loose motion making accurate control of the dipper or bucket impossible, thus resulting in lower shovel efficiency. The dipper sticks

Figure 1

are in reality steel box beams, measuring 6 by 12 in., fabricated of welded 1-in. thick steel plate.

After the operator has picked out the point of the bend from the stretched cord he marks out the point at which heat will be applied. This marked area is really vee shaped with the apex of the vee being on the inner side of the top and bottom of the beam. We have found that heating an area the size of the one marked will straighten the stick or beam 1/2 in. If less than this bend is required, we reduce the size of the section to be heated proportionately. If more bend is necessary, we mark off additional vees and heat them as required.

In Fig. 2 two operators are heating the marked out section. The vee shape of the heated area can easily be seen here. Both operators are using heavy multiflame heating heads with a number of small flames on each heating tip rather than a single large flame port as is usual on standard oxy-acetylene tips. Notice that the long side of the vee-shaped heated area is toward the outside of the bend. This method of straightening does not depend on jacks or physical force to straighten the member, but depends on the contraction of the metal after heating. The long side of the vee-shaped area be-

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Figure 2

Figure 4

ing toward the outside (convex side of the bend) contracts more than the apex side of the vee and consequently the outer or convex side of the stick is shortened when cooling is complete. After this heated section has cooled, the operator again rechecks the stick with piano wire or a cord stretched tight and repeats this procedure at the needed point. One, two or even three heats may be required to straighten a very badly bent stick.

The average time required to bring the section outlined in chalk to a red heat is 6 to 8 min, after which the beam is allowed to cool normally in air. It is important to use relatively large heating heads in this type of operation to confine the heat to the chalk-marked areas before appreciable amounts of heat can spread to adjacent parts of the beam. The inside surface of the beam is not heated at all.

The operators have nearly finished the heating operation in this view (Fig. 3) with only a little heat still required on the top and bottom of the stick.

The total time on the average job requiring the straightening of both sticks is approximately 3 hr, for an oxy-acetylene operator and helper. This time can be fitted in easily any time a shovel is idle, since no dismantling is required. Minimum downtime and max-

Figure 5

Figure 6

Figure 3

Figure 7

imum shovel efficiency are the goal of every quarry, and flame shrinking helps us to achieve both. This straightening work is a standard operation in our quarry.

Figure 4 is a flame-shrinking job of a different and heavier type. This view shows an eccentric or pittman shaft for a 24- by 36-in. Traylor Jaw crusher. This shaft measures 13 in. in diameter and is approximately 10 ft long, weighing 1 1/4 tons. The shaft becomes bent under heavy crushing conditions and must be straightened or replaced. The operator here has the shaft in a lathe and is marking out point of bend.

The point of bend has been determined and the area to be heated has been marked. This shaft is bent about 3/8 in. and will require 3 heats of the area shown for straightening.

Figure 4 shows the operators heating the marked out area. Again two heavy multiflame heating torches are used. It takes approximately 15 min to bring the area marked to a dull red heat (1500° F).

We have found that it is best to heat and then allow plenty of time for the shaft to cool naturally. Standard practice is to heat once a day, although when rushed, we have made heats every 4 hr. Three heats straightened this shaft to within twenty thousandths, which was within the required tolerances. Again no physical force was applied in this straightening work, the contraction of the metal in the heated area pulling the shaft true.

Figure 5 is still another flame-shrinking job. This view shows the Pittman rod or connecting rod on a Lincoln Granite Gang Saw. In producing dimension stone, the granite is removed from the quarry in blocks approximately 6 by 6 by 14 ft. Such a block is shown here in the gang saw. The gang saw cuts this block into finish slabs. The gang saw is equipped with blades and hardened steel shot is poured into the slot or kerf to produce cutting action. Average sawing time on such a block is 12 hr. The connecting or Pittman rod transmits the reciprocating or sawing motion to the saw blade cradle or frame. This Pittman rod is in reality a heavy I-beam measuring 15 by 8 in. Sometime ago this beam bent approximately 3 in. out of line.

Figure 8

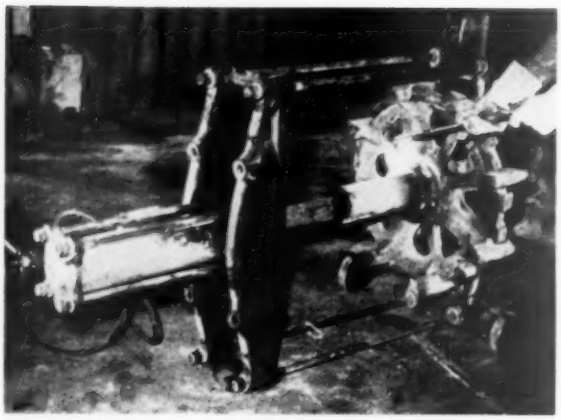


Figure 9

Figure 10

Figure 11

Using the same vee-shaped heats, as previously described for dipper stick work, this beam was straightened in a few hours using a total of 3 heats. The heated areas can be seen in Fig. 6.

In addition to "flame shrinking" we use heavy heating for a number of jobs. Figure 7 shows the mantle for a 36-in. Gyratory Crusher. The operators are getting ready to zinc the mantle to the bell. The housing is being heated to remove moisture and gasses which would cause porosity in the zinc when poured.

In Fig. 8 operators are bending an "eye" on a piece of shafting for a breakdown job. On breakdown work, we have found oxy-acetylene heating invaluable for shortening the downtime. The operators will have this "eye" shaped and back on the job before the forge could be lighted.

Figure 9 shows a portable hydraulic power press rigged to press the drive sprocket shaft from the sprocket of a Northwest 80D Shovel. Sometimes the job is too heavy for a portable press.

The quick heat from the oxy-acetylene flame expands the hub of the sprocket wheel before heat can soak into the shaft and the shaft is easily pressed out.

Oxy-acetylene braze welding also plays its part in our maintenance operations. Figure 10 shows an operator gouging out a crack in a Pittman cap preparatory to braze welding. These caps fit on the large eccentric shaft shown in the previous slides. These caps are water cooled and when the jackets crack the cap must be braze welded before re-Babbitting.

The oxy-acetylene gouging process provides fast easy way to vee out these cracks both outside and inside on the Pittman cap work.

The operator shown in Fig. 11 is brazing the cap. After braze welding the cap, the jacket is tested at 65 lb water pressure before proceeding with re-Babbitting.

Figure 12 shows the operator braze welding a cracked eccentric housing. By the use of braze welding for

Figure 12

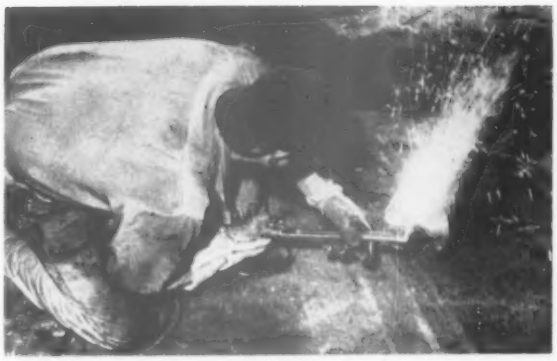


Figure 13

maintenance and repair, we are able to keep our stock of spare parts to a minimum and still operate without downtime waiting for spare parts.

The oxy-acetylene cutting processes play their part in the maintenance work required to keep the plant running smoothly. The oxy-acetylene gouging process has already been illustrated on a Pittman cap job. Figure 13 shows an operator trimming a churn drill bit. These bits are used to drill the 6 $^{3}/_{8}$-in. diam holes 60 ft deep required for our primary blasting. The inner courses or muck passages gradually close under repeated sharpening. The courses here are being trimmed to proper contour and depth.

The bits shown in the previous illustration are screwed into a heavy drill stem. These stems are subject to cracking and breakage at the point just above the bit under the constant abuse of drilling. A standard practice in our maintenance department is to arc weld on a new box joint end after preparing the stem by oxy-acetylene cutting.

Figure 14

Figure 14 shows the operator completing one of the two cuts required to get a chisel point. After both sides are beveled in a similar fashion the welding is completed and the stem put into spare parts stock.

Shown in Fig. 15 are liners used on the machines that sack the poultry grit produced in our plant. These

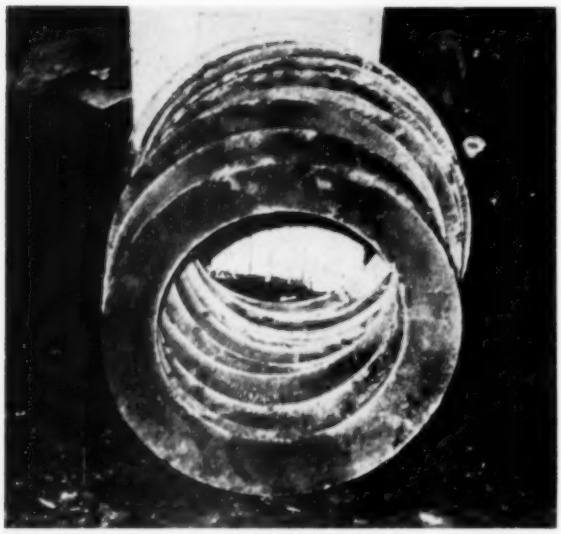


Figure 15

liners are subjected to extreme abrasive wear and require periodic replacement. These liners are first cut to approximate size with the oxy-acetylene flame.

Because of the close tolerances required the liners are then stacked and swung in a lathe for machining to final dimensions. The fabrication of these liners in our own shops has resulted in substantial economies in the maintenance of these sacking machines.

As a final illustration of how we use oxy-acetylene cutting processes in our maintenance, these two illustrations show the oxygen lance used for removing a shaft "frozen" in this spudding gear from a 27-T Bucyrus Erie Well Drill. This shaft resisted all efforts with a hydraulic press and oxy-acetylene heating. For those not familiar with lance technique, we will describe the operation briefly. The lance itself consists of a length of $^3/_8$-in. steel pipe hooked up to an oxygen regulator with a length of standard $^3/_8$-in. oxygen hose. A valve on the inlet end of the pipe controls oxygen flow. In Fig. 16 the operator is using a cutting blowpipe to bring the end of the lance pipe and a small spot on the shaft to a red heat. When this is accomplished, the lance operator slowly opens the oxygen lance valve.

The oxygen stream from the lance bores a hole through the center of the shafting as the lance pipe is advanced. Some of the lance pipe is consumed in the operation. After the hole is lanced through the length of the shaft, the shaft is cooled and easily removed.

Figure 17 shows the size hole lanced in the shafting. This method eliminates the danger of damaging the bore

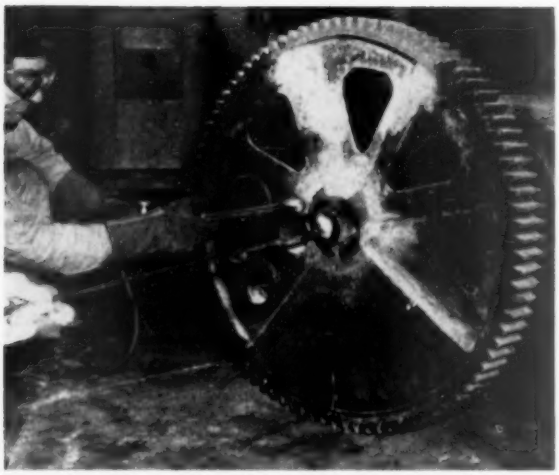


Figure 16

Figure 17

or keyway on the gear proper. The entire operation shown here took only a few minutes.

In the space allotted, it is obviously impossible to give a complete picture of the oxy-acetylene operations performed by our maintenance department. However, the figures shown do give some idea of the variety of jobs handled and the economies that can be realized by the full utilization of oxy-acetylene processes in quarry maintenance.

Tailors in Steel to Canadian Industry

by I. Mac Fadden

"BUILT BY JOHN INGLIS"—four words with the ring of familiarity for everyone in Canadian industry. They are to be found on countless pieces of equipment and installations in every basic industry throughout the country, ranging from small pumps, giant fractionating towers, to complete paper mills.

John Inglis Co. Ltd., Toronto, Ont., actually comprises many specialized industries, with comprehensive engineering and manufacturing facilities for the complete designing, construction and installation of one of the widest ranges of heavy industrial equipment handled by any one firm in Canada.

In the General Engineering Division of the company, the production of boilers, compressors, air conditioning and refrigeration equipment, glass-lined tanks and marine condensers are everyday matters. Here, too, are planned and constructed large, custom-built installations, such as digesters, heat exchangers, heavy specialized pressure vessels, papermaking machinery and huge hydraulic turbines for power plant installations. In fact, the complete list of projects and plants equipped with John Inglis installations reads like a roll call of Canadian industry.

I. Mac Fadden, Editor, "Liquid Air Review" published by Canadian Liquid Air Co., Ltd., Montreal, Canada.

Fig. 2 Oxy-acetylene flame cutting of heavy flanges from 3½-in. plate with multitorch automatic Airco Travograph machine

John Inglis has manufactured heavy machinery for many years, except during the recent war when the whole plant's output was devoted to war production. During this period, the plant was considerably enlarged, and many of the mass production techniques developed during those years are now being utilized to produce a

Fig. 1 Exterior circumferential seam being manually arc welded on Inconel lined mild steel digester cone, mounted on a revolving positioner

Fig. 3 Spiral casing for hydraulic turbine being fitted and welded to speed ring

Inlet pipe tapers from 5 ft diam to approximately 18 in. When completed this turbine will be shipped to an island in the South Pacific, and is typical of the installations built by John Inglis for export.

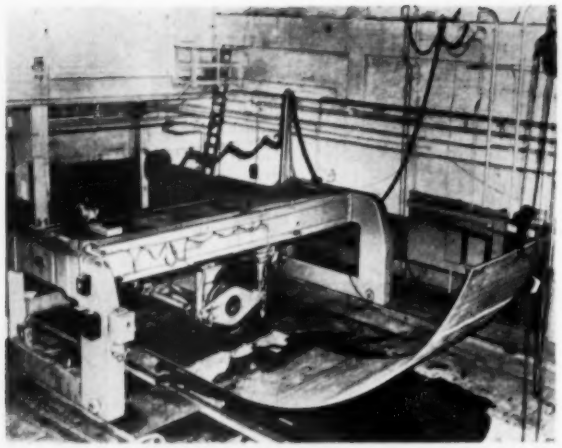


Fig. 4 Special machine, designed and built by J. Inglis engineers, for spot welding large stainless and Inconel alloy sheets to mild steel plates for subsequent fabrication into lined vessels

This technique enables John Inglis to overcome recurrent shortages of special steels by combining Canadian mild steel with alloy steels from the United States.

variety of consumer products. This new division of the company, established after the war, markets domestic washing machines, stoves, hot water heaters, cylinders for liquid petroleum gas and even fishing tackle and sporting arms. John Inglis' products are also exported to many different countries. In addition to producing these highly diversified lines, John Inglis are presently completing a new plant at Scarboro, outside Toronto, for important defence production of marine steam turbines.

In the main plant, occupying over half a million square feet, with about 2000 employees, the General Engineering Division is superbly equipped with all the necessary production tools and facilities, including row upon row of machine shop tools, a large sandblasting room, one of the largest stress relieving and annealing furnaces in Canada, and two exceptionally large plate shops. Fabrication of steel plate has long been a specialty at John Inglis, and the organizing ability, meticulous workmanship and progressive spirit of the plate division personnel would be a credit to any company.

Fig. 6 Alloy layering of main seams of the Inconel lined digester shell, 11 ft 6 in. diam by 54 ft long with the Aircomatic process

Fig. 5 Two massive all-welded flow valve bodies for a hydraulic turbine being fabricated from flame-cut parts

Hundreds of pounds of electrodes were required for these weldments in which the material is up to 4 in. in thickness. In the background at the right, an oxy-acetylene heating torch is used to bend corner plates on a marine condenser under construction.

Fig. 7 Completed Inconel lined digester for the pulp and paper industry, entirely welded, X-rayed and stress relieved

Fig. 8 Here the modern Argowelding process is employed for repairing a nonferrous casting

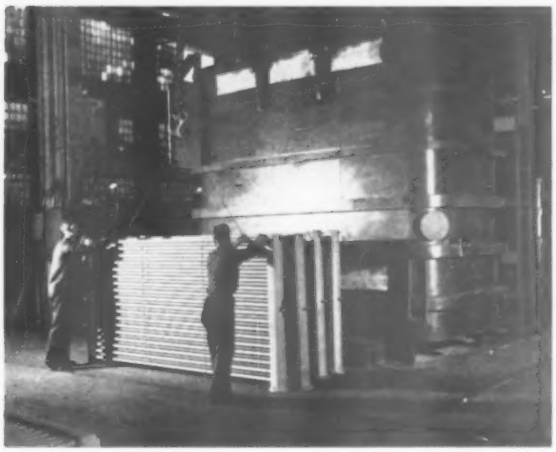


Fig. 9 Another standard J. Inglis product, 80,000-kva transformer tank with radiators, is readied for shipment to a new hydro installation. Tank and radiators are all-welded assemblies

Plates are cut to size by portable Radiagraph oxy-acetylene cutting machines, or on a huge 40-ft edge planer, to ensure uniform fit-up and good joint preparation for welding. For flame cutting plates into irregular shapes, a large multitorch Airco Travograph is continually at work. Welders are everywhere—the majority using the manual metallic arc process, others operating automatic submerged melt machines and still others using the newer inert arc processes of Argowelding and Aircomatic welding. In fact, in the perfection of techniques and procedures for the Aircomatic welding of stainless steels, the John Inglis Co., with customary initiative, is one of the most progressive companies in either Canada or the United States. At the present time it is manufacturing stainless steel pressure vessels that probably are among the first to be fabricated with the Aircomatic process in Canada. Oxygen and acetylene are distributed throughout the plate shops by internal pipe lines, the oxygen being supplied by L. A.'s bulk supply trailers, and the acetylene from a generator installation.

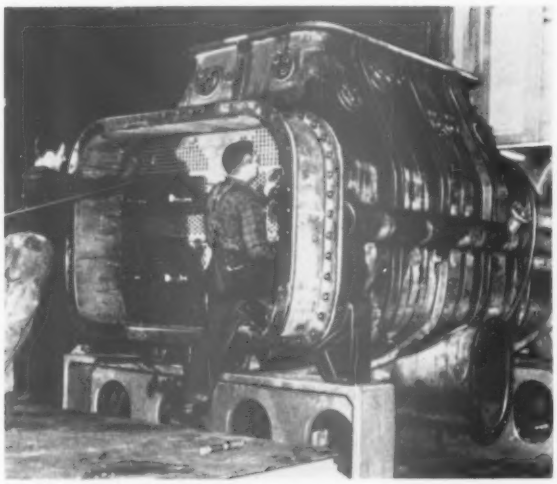


Fig. 10 Tubing a marine condenser for the world's largest freshwater tanker, the "Imperial Leduc." Condenser body was completely fabricated by manual arc-welding process

With its extensive engineering "know how," large-scale production facilities, and pride of craftsmanship, John Inglis undoubtedly will continue to advance still further in the forefront of heavy industry in Canada. Well deserved is the respect John Inglis has earned for undertaking many tough jobs turned down by other engineering firms, and converting each into another achievement "Built by John Inglis."

Braze Welding to the Rescue

by L. M. Smith

CHILDREN'S toys get rough treatment. This fire engine had a broken angle support between the rear frame and the hand-hold ladder support. Left unrepaired, the thin tubing would bend easily and the little vehicle would have been seriously damaged. But braze welding came to the rescue and the fire engine was quickly and easily repaired. Now the young man is ready for more "alarms."

There are a few fundamentals to remember if your braze weld is to be strong enough to withstand hard usage. Clean both edges of the break thoroughly, removing all paint, rust, dirt and grease. You can do a good cleaning job with a file or a wire brush. If the metal is thicker than $^1/_8$ to $^3/_{16}$ in., bevel the edges to provide a good welding vee.

Now heat the edges until they just begin to glow. At this moment, flow on bronze so that it tins the surface and runs down into the break. More rod metal should be added until the weld has been built up slightly higher than the surface of the part. When adding rod metal, make sure the molten bronze fuses thoroughly with previously deposited rod metal.

L. M. Smith, Linde Air Products Co.

Fig. 2 After tinning the edges with bronze, the weld was finished with extra metal for reinforcement. Oxweld flux-coated 25M bronze rod was used

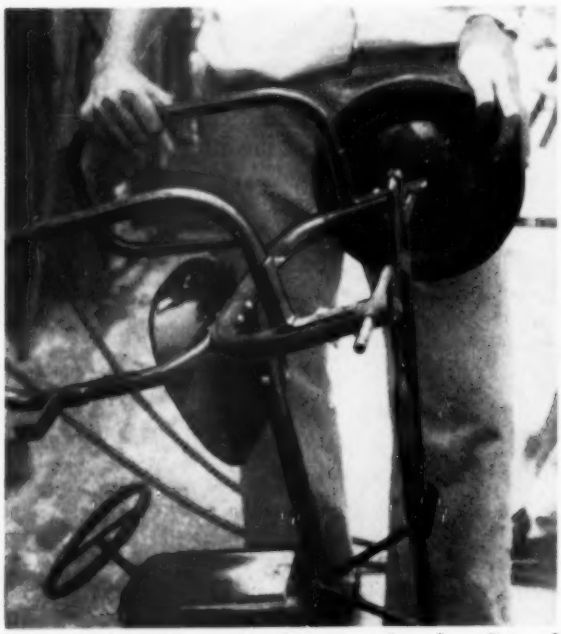


Fig. 1 A file and a wire brush were used to clean dirt and paint from both edges of the break which can be seen at the angle of the bracket

Fig. 3 The finished weld can be seen near the left rear wheel. Now this junior fire chief can play as hard as he wishes. The braze weld will hold

the AMERICAN WELDING SOCIETY

activities • related events

1954 SPRING MEETING AND EXPOSITION

Importance of Making Early Reservations

The National Spring Technical Meeting of the AMERICAN WELDING SOCIETY will be held in Buffalo, N. Y., Tuesday, May 4th through Friday, May 7, 1954, at the Hotel Statler which will be the AWS Headquarters Hotel.

Advance guest room reservation forms will be mailed to every AWS Member and contracted Exhibitor on Dec. 1, 1953. In order to accommodate Exhibitor requirements, the Housing Committee must give first consideration to Exhibitor companies' requests for Headquarters Hotel parlor suite accommodations and to paper Authors and Session Chairmen for room accommodations. Single room requests will be difficult to fulfill. Double or twin-bedded room requests will be processed faster. We suggest "team-up" arrangements from the standpoint of assured accommodation and economy. The Housing Committee will review all room reservation applications starting February 1st. No firm acknowledgments for accommodations will be made prior to February 15th. All applicants are advised to:

(*a*) Date mark their application form.
(*b*) Fill in answer to each question.
(*c*) Name all who will occupy rooms.
(*d*) Designate who will occupy same rooms.
(*e*) Specify date and hour of both arrival and departure.
(*f*) Specify accommodations required.
(*g*) Designate SOCIETY status correctly.

Failure to provide requested information can only result in delay and possible resulting loss of assured accommodation.

Applications will be honored on basis of date received. We urge all Members to send their applications completely filled out, to National Headquarters by return mail. Those residing more than 500 miles away should send their applications by Air Mail.

Every effort will be made by the SOCIETY to accommodate all who apply. But the rule of "first come—first served" is the only fair rule that can apply.

The AWS Welding and Allied Industry Exposition will be held at Buffalo, N. Y., Wednesday, May 5th through Saturday, May 8, 1954, at the Buffalo Memorial Auditorium.

Data, including exhibit floor plans, option-for-space forms, space prices, exhibit hall facilities, etc., will be mailed to the Industry Nov. 1, 1953, with request that industry send us its requests with covering option on first, second, third and fourth choice of space. The Committee will begin space assignment and send out Space Contract forms to all applying, starting Dec. 15, 1953.

Watch the columns of THE WELDING JOURNAL each month for important announcements and Tentative Program.

Walter H. Wooding

Wooding Wins Lincoln Gold Medal for 1953

The National Committee on Awards has selected Walter H. Wooding as the recipient for the 1953 Lincoln Gold Medal Award for his paper on "Inert-Gas-Shielded Metal-Arc-Welding Process." This paper was presented as an educational lecture series at the Annual Meeting of the AMERICAN WELDING SOCIETY in Philadelphia during the week of October 20, 1952, and was published in the April and May 1953 issues of THE WELDING JOURNAL.

Walter H. Wooding was born in Philadelphia, Pa., on Mar. 12, 1910, son of Capt. Walter Wooding, U. S. Marine Corps and Kathryn Reynolds Wooding. In addition to the public schools of Philadelphia and Fredericksburg, Va., he attended Staunton Military Academy, The United States Naval Academy and graduated from the Drexel Institute of Technology in Mechanical Engineering.

He entered the Naval civilian service in 1929 being employed in the Metals Laboratory of the U. S. Naval Engineering Experiment Station, Annapolis, Md., where he held various positions from Laboratory Helper to Physicist. In 1939 he transferred to the Industrial Test Laboratory of the Philadelphia Naval Shipyard as Plant Control Metallurgist. During 1941, he was appointed supervisor of the Metallurgical Engineering Group of the Laboratory; and, two years later became Superintendent of the Metals Section directing development and investigational projects concerned with the processing, fabrication and welding of hull materials used for Naval construction. From 1941 to 1943 he was also an Instructor in Advanced Metallurgy at the Temple University Evening Technical School, Philadelphia, Pa.

Since entering the field of Metallurgy nearly 25 years ago, he has authored a number of technical papers on the processing and welding of metals. Most of these were concerned with the metallurgy of welding low-alloy steels; and, the development and application of low-hydrogen type welding electrodes. He has appeared as a speaker before various Navy and civilian technical societies and organizations. In 1946, he presented a paper on "The Hardened and Tempered Microstructure of High-Speed Tool Steel as a Factor in Tool Performance" at the annual meeting of the American Society of Mechanical Engineers. At the annual meeting of the Compressed Gas Association in 1951, he presented a paper on "The Utilization of Inert-Gases for Metal-Arc Welding." He delivered an address on "Carburizing, Nitriding, Induction Hardening and Flame Hardening of Steels" before delegates to the Department of the Navy's 1952 Forge Practice Conference held at the Boston Naval Shipyard.

During the past ten years, Mr. Wooding has served on numerous Military and Civilian metallurgy and welding committees. He was Chairman (1945-46) of the Department of the Navy's Bureau of Ships Laboratory Committee on the Weldability of Steel, and served on both Army and Navy Advisory Committees for the Welding of Armor. More recently he has served on the Nickel Conservation Panel of the Minerals and Metals Advisory Board, National Research Council, and as a member of a Project Advisory Group of the Ship Structure Committee.

Mr. Wooding is a member of the American Society for Metals and the AMERICAN WELDING SOCIETY. He is a Professional Metallurgical Engineer registered in the Commonwealth of Pennsylvania. He is married to the former Dorothy Cloud Worrilow of Ridley Park, Pa.

Inspection Trips

Members visiting Cleveland will be given an opportunity to choose between two plant visits, one to the Cleveland Tank Plant of Cadillac Motor Car Division, GMC, and the other to the Euclid Road Machinery Co.

The visit to the Cleveland Tank Plant of the Cadillac Motor Car Division requires visitors to have proof of American Citizenship and is limited to 75 persons.

The visit to the Euclid Road Machinery Co. is open to all.

The visit to the Euclid Road Machinery Co. will include a trip through the plant showing trucks and tractors being assembled on a moving line and the testing of these machines on power-driven test rollers. The trip will also include a tour through the Machine Shop and Structural Shop where the welding of bodies and frames is done on turning positioners. Completed machines of various types will also be seen.

The visit to the Cadillac Cleveland Tank Plant, recently rehabilitated, will show how welding has played a large part in the modern production miracle. In fact, there are more people engaged in welding at the tank plant than in any other single job classification.

The actual production welding is done on "merry-go-rounds." These are similar to conveyors in that they bring the work to the man rather than having the man move to the work. The tack welded turrets and hulls are placed in positioners which move from one station to the next on a conveyor belt.

After the hull is welded it is sent to the 24-in. Betts boring mill where the turret race is machined. From there it goes to the Wean drilling machine where 100 holes are drilled simultaneously. It then moves to the Wean grinding machine where the area around these holes is ground to uniform size.

These operations and many others will be seen by WELDING SOCIETY members on October 20th when Cadillac will open its doors to the group.

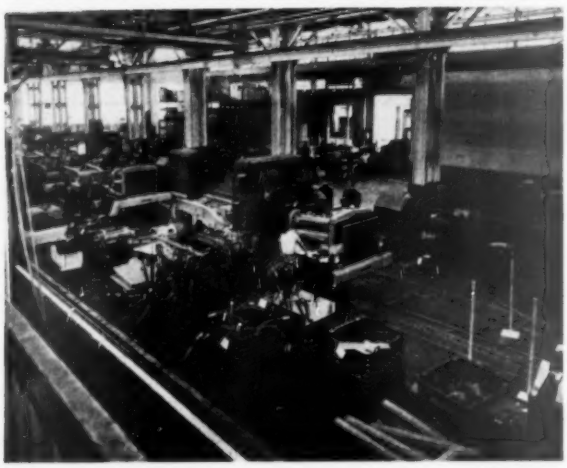
Previews of Visit to Cleveland Tank Plant, Cadillac Motor Car Division G.M.C.

Poised and ready for action is the Army's newest fighting weapon the T41 E1 Walker Bulldog

The final assembly line loaded with tanks. The T141, Gun Motor Carriage, is also assembled along the same line

A Quick Look at the Sights to Be Seen on Visit to Euclid Road Machinery Co. Plant

50 Ton rear dump Euclid on earth fill dam project

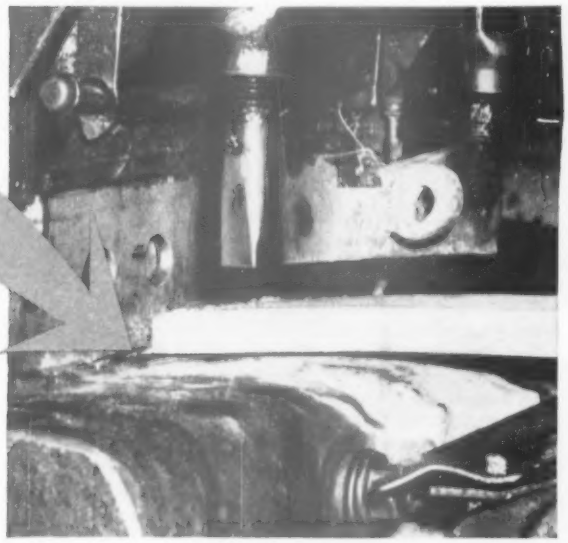


Assembly line production of 22 ton rear dump Euclids

HOT SHEAR BLADES STAY SHARP...when Hard-Faced with HASTELLOY Alloy C

TRADE-MARK

A $\frac{3}{32}$-in. deposit of HASTELLOY alloy C on the cutting edge of this shear blade has increased its life by four times. The edge can be rebuilt again and again, when it finally does wear.



This shear blade is used in a plant producing tough non-ferrous alloys. Before hard-facing was adopted, the blades would chip, lose their edge, and have to be scrapped after shearing only about 50 tons of metal. They now can handle roughly four times that amount of metal because they are protected with HASTELLOY alloy C. They can be machined and hard-faced again when they finally do wear. One set of hard-faced blades has been in use in the plant for more than three years with periodic maintenance.

In steel mills, too, HASTELLOY alloy C has increased the life of blooming mill shears by as much as 10 times. Hard-faced blades have lasted 110 turns without maintenance.

HASTELLOY alloy C rod has also been applied to many other hot-working parts with outstanding success. The metal flows well by metallic arc welding or HELIARC welding without preheating. No peening is necessary. Deposits of HASTELLOY C work-harden in service. They can be machined by conventional methods.

For information on how to apply HASTELLOY alloy C to hot-working parts, write for a copy of "HAYNES Hard-Facing Manual." For on-the-job help in applying the rod, get in touch with the nearest District office.

HAYNES *alloys*

TRADE-MARK

"Haynes," "Hastelloy" and "Heliarc" are trade-marks of Union Carbide and Carbon Corporation.

Haynes Stellite Company
A Division of
Union Carbide and Carbon Corporation
UCC
General Offices and Works, Kokomo, Indiana
Sales Offices
Chicago — Cleveland — Detroit — Houston
Los Angeles — New York — San Francisco — Tulsa

NEW PRODUCTS

Conductivity Test

Introduction of the Magnatest FM-100 Conductivity Meter has, by permitting rapid measurement, made the physical property of conductivity a practical and useful industrial tool. The need for wire-like samples or cross sectioning of parts is eliminated. The FM-100 gives absolute conductivity measurements of nonferrous conducting materials directly from a large dial on the front of the unit. An operator merely places the simple hand probe on a part and reads the dial. All that is required is a relatively flat area about $^3/_8$ in. in diameter on which to place the probe.

The other general category of applications depends upon the relationship between conductivity and other physical properties. Magnaflux Corp., 7300 W. Lawrence Ave., Chicago 31, Ill.

Eutectrode 69

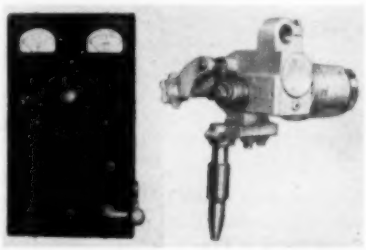
Particularly recommended for the joining of light sections of low- and medium-alloy steel and stainless steel Types 301, 302, 320B and 303, new EutecTrode 69AC and 690-DC featuring superb usability characteristics has been announced by Eutectic Welding Alloys Corp., Flushing, N. Y.

These new developments, the manufacturer reports, offer greater alloy deposition per electrode, produce smooth running deposits of excellent appearance and, in addition, permit swift, easy slag removal.

New EutecTrodes 69AC and 690-DC are available in the following sizes: $^3/_{32}$, $^5/_{32}$ and $^3/_{16}$-in. For further technical data, prices, etc., write to Department "P," Eutectic Welding Alloys Corp., 172nd St. and Northern Blvd., Flushing, N. Y.

Automatic Welding Head

A new and vastly improved Model 944 automatic welding head has been placed on the market by the Mir-O-Col Alloy Co., 312 North Avenue 21, Los Angeles 31, Calif.



Control of both welding head and positioner operation is automatic in the 944, as well as the high-frequency pilot circuit, if one is used. The electromechanical control circuit is designed to stop both head and positioner automatically if arc is broken or electrode sticks to the workpiece. A control cuts off high-frequency unit when arc is established. The motor drive is electrically independent of positioner and welder power supply.

The feed rolls on the welding head are of hardened steel and adjustable for both solid and tube wire and they accommodate any rod size from $^1/_8$ to $^1/_4$ in. in diameter. Hold-down roll has integral tension arm eliminating springs.

The unit is designed to operate with any positioner or transformer; maximum current capacity is 2000 amp.

A multicolored bulletin describing the 944 automatic welding head and positioner in full detail is available upon written request to the Mir-O-Col Alloy Co., 312 North Avenue 21, Los Angeles 31, Calif.

Low-Hydrogen Electrode

Weldbest 8016 is a new low-hydrogen type electrode with 3% nickel in the weld deposit, designed for a-c and d-c reversed polarity welding. Charpy impact tests at −150° F gave the following results:

(*a*) Notch in the weld, ft-lb: 24.0, 23.0 and 22.5.

(*b*) Notch in the heat-affected zone, ft-lb: 32.0, 29.5 and 33.0.

Further information and samples on request from Weldwire Co., Inc., N. W. corner Emerald & Hagert Sts., Philadelphia 25, Pa.

Fleetweld 180

A new electrode for all-position welding with a-c or d-c, Fleetweld 180, has been developed by the Lincoln Electric Co. of Cleveland, Ohio. It is specially designed for people who are not professional welders but who do repair work or other intermittent welding jobs. Welders who experience difficulty in welding overhead and vertical will find this new electrode helpful. It has fast-freezing characteristics and creates little slag to interfere with the arc and crater when welding vertically.

Fleetweld 180 is designed for use with the low open-circuit voltage, limited input a-c transformers type welders which have become so popular on farms and for maintenance welding. It can, of course, also be used on all other types of welders. It is well suited for welding of galvanized material or for welding of rusty, dirty or lightly painted material. The penetration is deep both with a-c and d-c and, when using a d-c welder, either polarity can be used.

The new electrode is being manufactured in two rod sizes: $^1/_8$ and $^5/_{32}$ in.

Welding Engineers: Two responsible positions are available for outstanding men to do fundamental and applied research work in the following fields:

(a) Insert gas welding, resistance welding, and mechanical aspects of joining.

(b) Soldering, brazing and gas welding.

Several years' experience in welding research would be an asset. Living conditions are excellent and salaries commensurate with ability. Send details of scholastic and industrial experience to Joining Department, Division of Metallurgical Research, Kaiser Aluminum & Chemical Corporation, P. O. Box 1451, Spokane, Washington.

Advertising Rates

The Welding Journal

Effective March 1, 1953	1 time	3 times	6 times	12 times
Full Page	$255.00	$235.00	$220.00	$200.00
Two-thirds Page	180.00	165.00	155.00	145.00
Half Page	140.00	130.00	120.00	110.00
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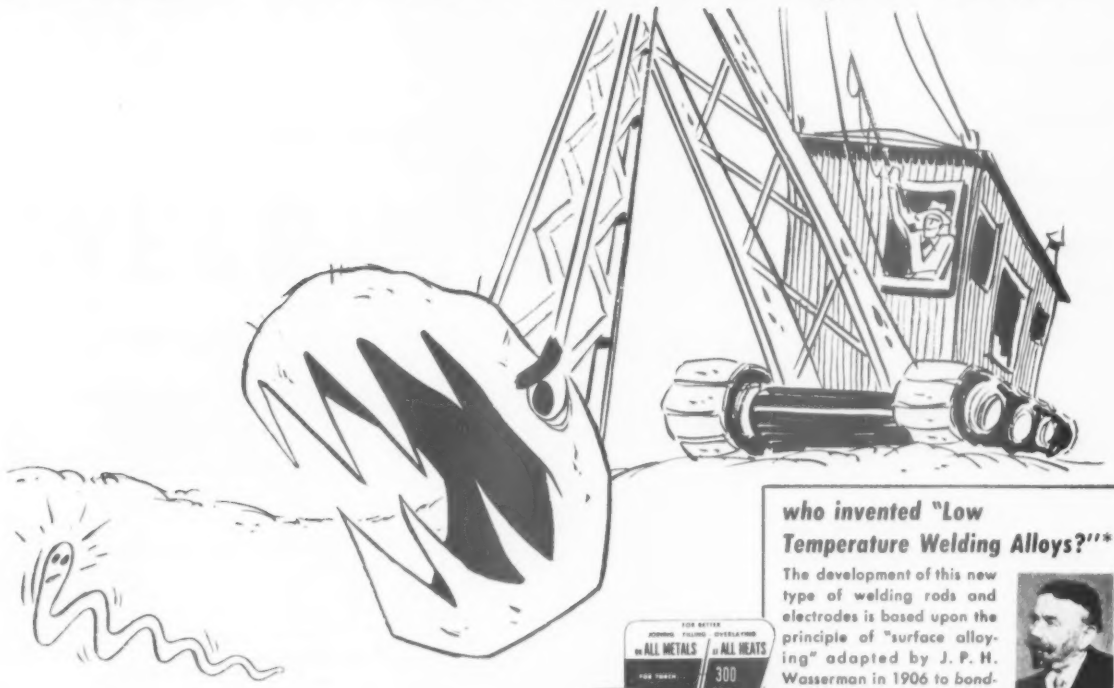
you wouldn't use a steam shovel to dig for bait...



You wouldn't use a steam shovel to dig for bait—why waste valuable time, labor, and materials trying to make one welding alloy "do" for a multitude of different kinds of joints that demand *specific* "tools for the job"?

EUTECTIC offers a complete range of "Low Temperature Welding Alloys" each carefully formulated to do a *specific* job "Better—Faster—Cheaper"—while at the same time avoiding the stress, warpage, distortion, embrittlement and weld failures that conventional high-heat methods invite.

If you're still using "make do", old-fashioned joining techniques in *your* shop, it will pay you to investigate these new EUTECTIC speeds and savings TODAY.

who invented "Low Temperature Welding Alloys?"*

The development of this new type of welding rods and electrodes is based upon the principle of "surface alloying" adapted by J. P. H. Wasserman in 1906 to *bonding below fusion heat of any base metal.* Research was continued by his son, Rene D. Wasserman, which resulted in Eutectic's new process of welding, giving superior bonds at temperatures far below the melting point of the base metal.

Today, these world famous welding alloys are used in over 78,000 plants in America and are sold and serviced in 67 countries. Industry throughout the World has adapted this process that minimizes risk of distortion, embrittlement, stress, and the other ill effects of conventional welding and brazing.

Low Temperature EUTECTIC WELDING ALLOYS GIVES YOU 160 JOB PROVED "Low Temperature WELDING ALLOYS"* TO ANSWER YOUR SPECIFIC METAL JOINING NEEDS...ON ALL METALS...AT ALL HEATS!

Cast Iron • Steel • Copper • Brass • Bronze • Stainless Alloy Steels • Nickel • Die Cast • Aluminum • Magnesium

Low Temperature EUTECTIC WELDING ALLOYS
BETTER, FASTER, CHEAPER

...at your service...

Over 350 trained District Engineers and auxiliary sales personnel are available to you from coast to coast for FREE Consultation-Demonstration in your shop . . . without cost or obligation!

FREE
77-Page Book!
"Manual of Welding Design and Engineering"
with 50 new photos, 60 new drawings. Send for YOUR free copy TODAY

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SEE ACTUAL DEMONSTRATIONS AT THE NATIONAL METAL EXPOSITION...

CLEVELAND, OHIO...OCTOBER 19-23

Low Temperature EUTECTIC WELDING ALLOYS **BOOTH 1606**

A SURE WAY to get fast, low-cost metal joining...

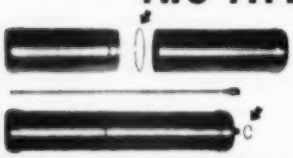
PREPLACE

THE BRAZING ALLOY

ALLOY PREPLACEMENT is a key factor in the tried and proved EASY-FLO and SIL-FOS low-temperature silver alloy brazing formula that gives you *any brazing production you want* at surprisingly low cost. It's a simple abc formula... where a = assembling parts with EASY-FLO or SIL-FOS *pre*placed at the joints... b = using a fast heating method such as oxyacetylene torch, furnace, electrical induction, gas-air burners, etc.... c = devising a production set-up that keeps assemblies moving steadily to and through the heating station. This formula eliminates the human element from the actual brazing. It assures fast, reliable production with unskilled labor.

EASY-FLO and SIL-FOS are available in rings, washers, shims and other preformed shapes to suit specific joint requirements.

TWO TYPICAL EXAMPLES



NAVY BUZZER CASE COVER

8 posts and gasket brazed to cover with EASY-FLO wire rings preplaced. Assemblies go thru furnace 1500 a day.

INSECTICIDE BOMB

Steel shell stampings and copper vapor tube joined with rings of EASY-FLO preplaced. Two girls turn out 120 bombs per hour.

A BRAZING PRODUCTION EXPERT IS AT YOUR CALL

Without obligation one of our field engineers will call and tell you if, where and how you can use the preplacement formula to advantage. Write and say when. *FOR FULL FACTS IN PRINT* write for a copy of BULLETIN 20...

HANDY & HARMAN — FOUNDED 1867 — ORIGINATORS OF EASY-FLO & SIL-FOS — QUALITY SERVICE

HANDY & HARMAN

General Offices: 82 Fulton St., New York 38, N. Y.

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AT THE METAL SHOW—CLEVELAND—OCT. 19-23

SEE an actual application of the preplacement formula in action... Also many more examples of EASY-FLO and SIL-FOS production-brazed assemblies... *TALK* over your metal joining problems with our experienced engineers.

BOOTH 941

"Root Protection" Flux

Especially designed for stainless steels where in normal operations the root (backside) of an inert gas joint remains unprotected. Previously, considerable oxidation occurred requiring painstaking grinding to prevent corrosion. The new free-flowing root protective Eutector Flux 569-I is claimed by the manufacturer, Eutectic Welding Alloys Corp. of Flushing, N. Y., to be the most advanced step in solving this problem of inert-gas welding. This new product is thus a backing flux for stainless steels, and also nickel, Monel, Inconel and the "super" alloys. When applied to the underside of butt and corner joints, penetration is simplified by the new flux action in controlling the molten weld metal. Thus, over-all underbead protection is assured.

Additional data may be obtained by writing Department "P," Eutectic Welding Alloys Corp., Northern Blvd. and 172nd St., Flushing, N. Y.

Silver Solder Brazing Flux

Having already produced several silver solder brazing fluxes, Superior Flux & Manufacturing Co. has developed and tested an improved flux formula to meet the demands of manufacturers who found workers objecting to brazing because of toxic conditions. In this new formula called Special No. 6 Silver Solder Paste Flux, Superior has produced a flux, which overcomes these objections and has an unusual combination of fluxing advantages. One of the most important factors is that it works equally well at both the highest and lowest melting points encounted in brazing operations. As a result it can be used for brazing, hard soldering and silver soldering any ferrous or nonferrous metal or alloy except aluminum and magnesium. Other features claimed for this product are as follows: Wide temperature range; melts at 850° F and is persistent and effective to 1600° F; no fumes, contains no free fluorides, guaranteed noncorrosive. Soluble in water: Simply use hot water and brush to remove flux residues. No grinding or chipping necessary. Reduces cleaning expense.

Manufacturer will supply sample free of charge for testing purposes. Write to the Superior Flux & Manufacturing Co., Department X-11, 1302 Ontario St., Cleveland 13, Ohio.

AWS NATIONAL FALL MEETING

Week of October 19, 1953

WORTH WAITING FOR!

WELD-ARC LOW HYDROGEN ELECTRODES

For Welding with AC-DC in All Positions

WELD-ARC TYPES	TYPICAL WELD DEPOSIT ANALYSIS
7016	.70% Mn
7016 N	1.0% Ni
8016 N	3.30% Ni
9016 CM	1.0% Cr—.50% Mo
9016 MM	1.70% Mn—.40% Mo
9016 NCM	1.35% Ni—.55% Cr—1.40% Mn
10016 NM	1.70% Ni—1.35% Mn
10016 CM	2.40% Cr—.50% Mo
12016 NMV	1.75% Ni—1.25% Mn—.25% V

A FULL AND COMPLETE LINE OF NINE DIFFERENT LOW HYDROGEN ELECTRODES

HERMETICALLY SEALED in 10 lb. moisture-, dirt-, grease- and dust-proof all-metal containers immediately upon emerging from the baking oven.

SECURELY PACKED in oven-fresh condition, with 6 containers carefully cushioned from each other by inner carton; then placed in a sturdy outer carton to prevent possibility of any damage en route to you.

Send for your free copy of

THE FACTS ABOUT LOW HYDROGEN ELECTRODES

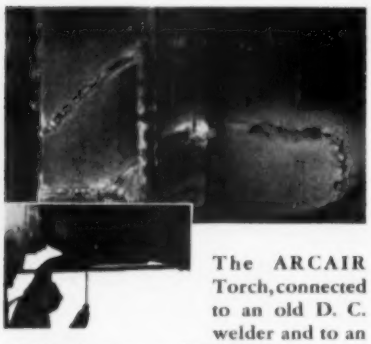
ALLOY RODS
COMPANY
YORK, PENNSYLVANIA

NO FINER ELECTRODES MADE... ANYWHERE

IN CANADA • Distributed by Canadian Liquid Air Company, Ltd., with Branches, Warehouses and Depots in all Principal Canadian Centers.

ARCAIR gouging torch helps save six months "down time" for Ohio grading contractor.

H. W. Stepler of Rocky River, Ohio was faced with six months "down time" and a $1200 repair bill on his 14-ton grader. A sheared bolt had shattered the cast iron transmission case into a jig-saw puzzle of cracks . . . only a heavy chain and turnbuckle held the broken case together.



The ARCAIR Torch, connected to an old D. C. welder and to an air line from an ordinary portable compressor, did a fast job of gouging all the cracks in less than two hours . . . a job that would have taken several days by other methods. The resulting clean, uniform "V" groove provided good weld penetration . . . after ten months there is no sign of cracking.

Total cost, excluding welding time, was $20, a saving of over $1000 and six months "down time".

Write for Free Bulletin with facts and photographs of other time and money-saving applications of the ARCAIR Torch. Arcair Company, EASTERN DIVISION, 423 So. Mt. Pleasant Ave., Lancaster, Ohio; WESTERN DIVISION, P.O. Box 4107, Bremerton, Wash.

Arcair TORCH

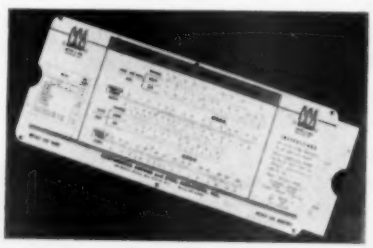
Cuts all metals—using only electric arc and compressed air

Aluminum Fluxed Studs

A new type welding stud incorporating several important new features is currently being manufactured by Shielded Stud Welding Co., under patents pending. A thin aluminum coating is applied over the entire surface of a mild steel stud. This coating acts both as an effective flux for the welding end of the stud and also protects the rest of the stud, particularly the threaded portion, from chemical attack and rusting. An added feature of a fully threaded type is that such studs may be welded at either end. Full information may be obtained by writing to Shielded Stud Welding Co., 1540 Seabright Ave., Long Beach 13, Calif.

New Slide Rule

A convenient new tool for the metal-working industries has been designed by Continental Copper & Steel Industries, Inc., for distribution by its various divisions in the metal-working field.



Through simple manipulation of a slide, the new CCS computer provides weights of strips, sheets, bars and plates of various metals and alloys. Factors are given for steel, stainless steel, nickel, Monel, Inconel, aluminum, Hasteloy, copper and titanium. On the reverse side is a condensed version of the ASME Code giving basic specifications for the fabrication of pressure vessels.

The device is available through Alloy Fabricators Division, Perth Amboy, N. J.; Braeburn Alloy Steel Division, Braeburn, Pa.; Niagara Falls Smelting & Refining Division, Buffalo, N. Y.; Walsh Holyoke Boiler Works Division, Holyoke, Mass.; Welin Davit and Boat Division, Perth Amboy, N. J.; or the main office of Continental Copper & Steel Industries, Inc., 345 Madison Ave., New York 17, N. Y.

Powder Lance

Powder lancing combines the advantages of the powder-cutting process with the ease and efficiency of oxygen lance operations. The introduction of metallic powder into the oxygen stream produces an exothermic reaction hot enough to pierce holes in many materials that resist the standard oxygen lance. Blast furnace salamanders, concrete, cinder blocks, fire brick, aluminum billets, steel and cast

ALLOYS All-State FLUXES

HOW TO DO IT

Condensed Versions of ALL an Operator Needs to Know to Swing to

EASIER WAYS

of Welding, Brazing, Soldering, Tinning, Cutting

FREE!!

Which one do you want?

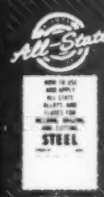
All-State Welding Alloys Co., Inc., White Plains, N. Y.

I've checked the "How to Use and Apply" folder I want.

■ DOC ALLOYS KIT — Generous sampler of 11 torch applied ALL-STATE alloys, 8 fluxes, 1 galvanizing powder.

■ ALUMINUM and ALUMINUM ALLOYS — World's most complete line of aluminum alloys and fluxes — ALL-STATE.

■ COPPER and COPPER-BEARING ALLOYS — 10 ALL-STATE alloys and fluxes for work on copper and copper-bearing alloys.



■ STEEL — 13 ALL-STATE alloys and fluxes for work on STEEL.

Name_____

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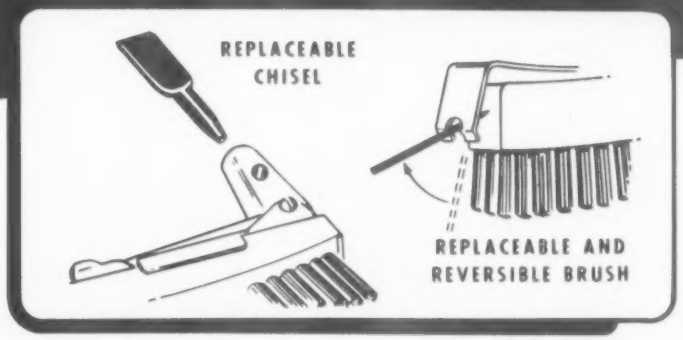
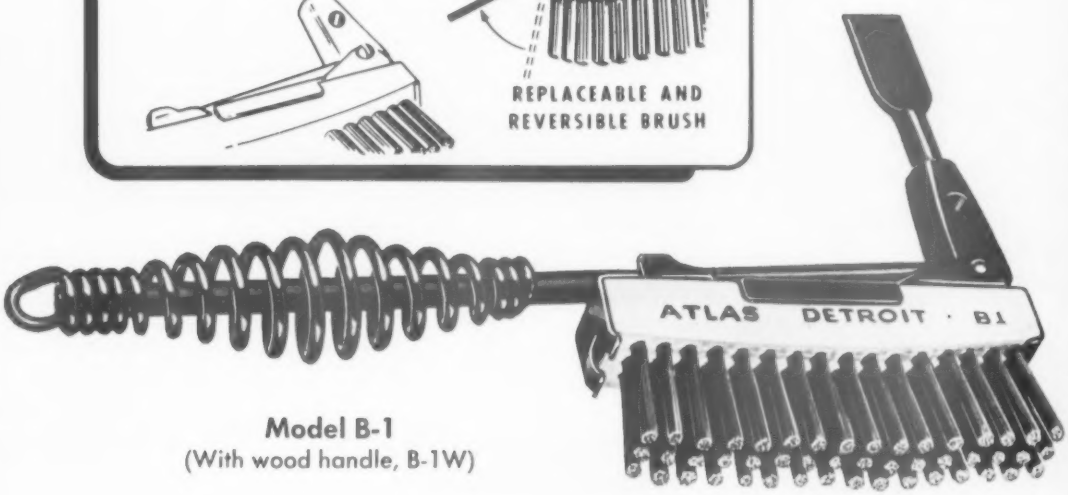
THE MOST PRACTICAL WELD CLEANING TOOL EVER DESIGNED!





Model B-1
(With wood handle, B-1W)

Atlas "RE-BIT DUAL TOOL"

● Experience real economy with this tool that requires replacement of worn parts only! The "Re-bit" chisel can be set at any angle, has a wider edge and narrower cross section, gets into corners and pockets better, affords better vision. "Re-bit" is easier to sharpen, lasts longer . . . Ask your welding supply dealer for information on all 27 Atlas models.

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THE SYMBOL OF QUALITY

WHY TAKE LESS?? TAKE THE BEST!!

PIONEERS IN PROCESSED WIRE FOR INERT GAS WELDING

"Chemically Processed" Microspooled "Quality Controlled" Wire for All makes of Inert Gas Welding Equipment will provide proper wire feed and quality welds

WELDSPOOL

ELECTRODE or FILLER WIRE for INERT GAS SHIELDED ARC WELDING

ALSO

WELDBEST Covered Electrodes
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Prompt Service on Standard or Specially Engineered products for your particular application

Ask for your nearest WELDWIRE DISTRIBUTOR

WELDWIRE COMPANY, INC.
N. W. Cor. Emerald & Hagert Sts.
Philadelphia 25, Pa.
Phone: GArfield 3-1232

iron containing inclusions, and sand incrustations inside large castings—all yield quickly and easily to the powder lance.

The Oxweld ACL-1 Powder Lance, introduced by Linde Air Products Co., a Division of Union Carbide and Carbon Corp., automatically mixes the oxygen and powder in the correct proportions to insure efficient use of this new process. Any standard lance pipe, in single or multiple lengths, can be used with this lance. Positive-pressure powder control assures excellent piercing results with multiple lengths up to 63 ft.

One simple lever controls the flow of both oxygen and powder. A convenient thumb latch keeps the valves open as desired, allowing the operator use of both hands for lance manipulation. The oxygen and powder tubes are fully enclosed to protect them from external abuse.

The lance pipe holder works like a drill chuck; a twist of the wrist opens the holder and a reverse twist locks the lance pipe in place and seals it gas tight. Three interchangeable holder sizes are available: to fit $^1/_4$-in., $^3/_8$-in. and $^1/_2$-in. pipe.

Dispenser for Powder Lancing

Powder lancing uses an extremely high temperature to pierce holes in many materials that resist the standard oxygen lance. Effective powder lancing relies upon a continuous, steady flow of airborne metallic powder through the lance to the

The Oxweld D-9 Powder Dispenser supplies exactly the right amount of metallic powder for maximum efficiency

AT THE FRONTIERS OF PROGRESS YOU'LL FIND

AN AIR REDUCTION PRODUCT

For Welding and Cutting . . .

NATIONAL CARBIDE

in the red drum

HIGHEST QUALITY
DUST FREE

Write for the name and address of the NATIONAL CARBIDE supplier nearest you.

National Carbide Company

GENERAL OFFICES: 60 EAST 42ND STREET, NEW YORK 17, N. Y.

A DIVISION OF AIR REDUCTION COMPANY, INCORPORATED

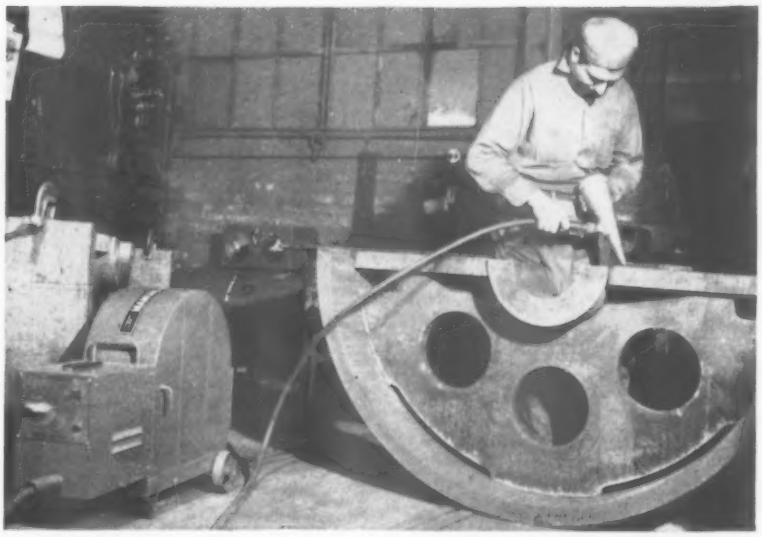


Fig. 3. MANUAL LINCOLNWELD *high-current density provides all the benefits of quality, low cost "hidden-arc" automatic welding . . . yet retains ease and simplicity of hand welding as it*

SIMPLIFIES AND SPEEDS WELDING TO CUT SHOP COSTS

Cuts Costs in Half. You weld 3 to 4 times faster, produce stronger, higher quality welds with "Manual Lincolnweld's" high-current-density welding. Welds are smooth, easy to clean and free of porosity. Less time required for welding, for joint preparation and weld cleaning add up to cut welding costs an average of 50% on many jobs.

Eliminates Joint Preparation. By concentrating up to 600 amps on a $\frac{5}{64}$" electrode, "Manual Lincolnweld" gives 100% penetration butt welds on plates up to $\frac{3}{4}$" thick.

Saves on Setup. Simpler than hand welding with coated electrodes, "Manual Lincolnweld" feeds its continuous wire electrode automatically. Granular flux from the cone reservoir of the welding gun keeps the arc submerged . . . prevents arc rays, minimizes smoke to make welding easier. The welding gun can be operated manually or mounted in simple fixtures to provide the benefits of automatic welding with the flexibility of manual welding.

Simple to Operate. "Manual Lincolnweld" is compact, portable . . . moves about the shop on wheels. It operates from a Lincoln "Shield-Arc" 600 amp DC welder.

START CUTTING COSTS TODAY. "Manual Lincolnweld" procedures and speeds are presented in Bulletin 1303. Write on your letterhead to Dept. 1910.

THE LINCOLN ELECTRIC COMPANY
CLEVELAND 17, OHIO

THE WORLD'S LARGEST MANUFACTURER OF ARC WELDING EQUIPMENT

MANUAL LINCOLNWELD CUTS WELDING TIME 50%

"Hidden-Arc" welding using "Manual Lincolnweld" is cutting welding time 50% and more on many components for Gar Wood earth movers. On some jobs, overall welding time has been reduced as much as 67% because of less warpage, lower set-up time and greater freedom from weld spatter.

Strength and quality of welds produced with "Manual Lincolnweld" are equal to fully automatic welding. Setup time, however, is less since the work can be welded in the tacking position, and the welding gun of "Manual Lincolnweld" can be used in any flat or near-flat position.

In the fabrication of these Gar Wood side frames, two men produce one pair of frames in only 8 hours. Each frame calls for 174 feet of welding. With hand welding, 40 manhours were formerly required to do this work.

Duplicate benches are used for fabricating the right and left side frames. Two men working as a team set up, tack, and weld, using the "Manual Lincolnweld" for both "hidden-arc" and open arc welds.

Fig. 1. Welding Manhours 60% Less *on side frame for earth mover assembly. With "Manual Lincolnweld", sides are finished in 16 manhours . . . formerly took 40 manhours, a 60% saving in time. Each side requires 174 feet of welding.*

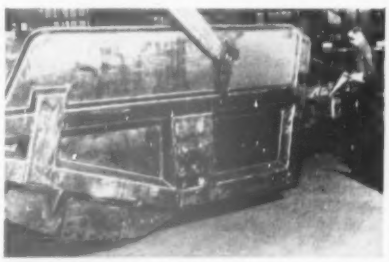


Fig. 2. Simpler to Set Up. *Components for side frame are tacked in position and welded with "Manual Lincolnweld". Joints too steep for "hidden-arc" welding are welded with coated electrodes.*

reaction zone. The Oxweld D-9 Powder Dispenser supplies this flow—at adjustable rates to provide maximum efficiency.

This high-pressure dispenser, introduced by Linde Air Products Co., a Division of Union Carbide and Carbon Corp., has a 200-lb powder capacity so that frequent charging is unnecessary. It delivers powder at conveying-air pressures up to 100 psi to enable the powder lance to be used effectively with multiple pipe lengths coupled together. Even when the lance is operating at extended distances, the air pressure and powder flow supplied by the dispenser are high enough to insure piercing ability as good as at the shorter distances. A relief valve on the dispenser vents at 100 psi for operating safety.

Although this dispenser has been designed especially for multiple-pipe use with the Oxweld ACL-1 Powder Lance, it meets the requirements for all scarfing and cutting processes that use powder. Powder is delivered from the dispenser to the lance through a standard $^1/_4$-in. rubber hose. A removable screen in the hopper sifts out powder lumps or foreign matter that might clog the ejector unit or powder passage. The dispenser is 40$^1/_4$ in. tall, has an outside diameter of 12$^5/_8$ in., and weighs 94 lb uncharged.

Cast Iron Sealing Rod

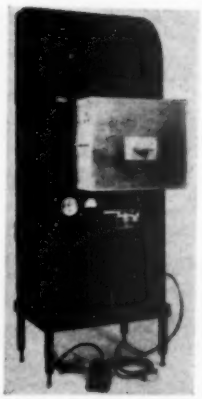
EutecRod 115, an extremely low melting point alloy permitting wide use on cast iron for sealing cracks and for building up worn and poorly machined surfaces where strength is not required, has been announced by Eutectic Welding Alloys Corp., Flushing, N. Y. Similar in many respects to Eutectic's widely used EutecRod 15, this new product is said to offer many new advantages in lower bonding heat, quicker tinning action, longer working range and easier spreading. Since it maintains the excellent adhesion and surprisingly powerful action of EutecRod 15, the new alloy, it is claimed, takes the hazard of poor results out of this type of sealing operation. It can also be used for bronze, steel castings and for filling and bonding any metal except aluminum and magnesium.

Available in a full range of sizes. For further information, technical data, etc., write Department "P," Eutectic Welding Alloys Corp., Flushing, N. Y.

Silver Alloy

Newly created EutecRod 1602, which features unusual "flowing" characteristics on tungsten carbide, difficult to braze alloys and steels, etc., has been announced by Eutectic Welding Alloys Corp., Flushing, N. Y.

It is claimed new EutecRod 1602 flows smoothly and easily even at elevated



temperatures, permitting the use of silver brazing-type joints on *all* "tough-to-bond" alloys which have refractory oxides of chromium, vanadium, nickel, etc.

In addition, this new product features the outstanding advantage of considerably lower heat input required for "T" and butt joints where a "white" solder-type alloy is desired on stainless steel. This new rod is available in $^3/_{64}$, $^1/_{16}$, $^3/_{32}$ and $^1/_8$ in. diameters. For further details, prices, etc., write Department "P", Eutectic Welding Alloys Corp., 172nd St. Northern Blvd., Flushing, N. Y.

MANGANAL

U.S. Patents 1,876,738—1,947,167—2,021,945

11%-13½% MANGANESE-NICKEL STEEL

Costs Less and

OUTLASTS NEW TRACTOR PARTS

• Manganal is the toughest metal known—minimizes wear from impact and abrasion.

free— SEND FOR COMPLETE DETAILS ON THE SPEEDY AND ECONOMICAL REPAIR OF TRACTOR PARTS.

Catalog and Price List Available

Mill Depots: Newark; Wilkes Barre (Forty-Fort) Pa.; Indianapolis, Ind.; Minneapolis, Minn.

STULZ-SICKLES CO.

SOLE PRODUCERS 92 N. J. RAILROAD AVE. NEWARK, N. J.

NEAREST DISTRIBUTOR UPON REQUEST

Induction Soldering Machine

A special induction heating unit designed specifically for soft soldering and silver brazing is announced by Radio Frequency Co. of Medfield, Mass. Model 5000 "RF Heater" incorporates a unique circuit and output transformer arrangement, permitting extremely high no-load circulating currents in the work coil. This causes a rapid heating of small hard-to-load-into parts. It also permits the use of single-turn or even half-turn work coils which the operator or the work can safely touch during operation. This gives a convenience which permits rapid loading and high production.

The machine is ideally adapted to such operations as sealing of condenser cans, and on similar products such as relays, thermostats, crystals, resistors, transformers, etc., where hermetic soldering is required. Hand soldering is eliminated. With Model 5000 soldering machine unskilled help can produce high quality work. For example: One operator can easily produce 500 to 1500 soldered condenser cans per hour with simple, inexpensive fixtures. The same machine is also suitable for soldering glass metal terminals, and can be set up for a new job in a matter of minutes.

Further information about Model 5000 induction soldering machine may be ob-

more manufacturers are using SW-15 electrodes than ever before!

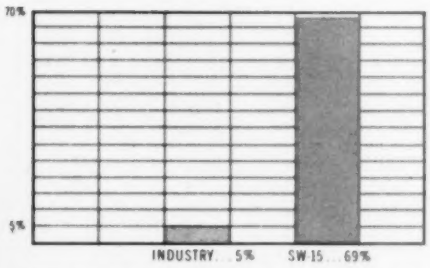
FASTEST GROWING ELECTRODE

The chart shows the increase, from 1950 through 1952, in shipments of electrodes. And the A. O. Smith SW-15 leads the industry's increase by 13.2 times!

more manufacturers are using SW-15 electrodes than ever before!

SW-15

the UNIVERSAL Electrode

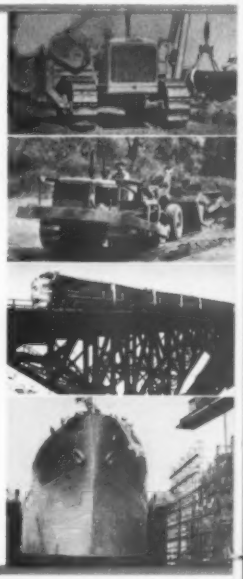


Here is the evidence of SW-15's growing acceptance among manufacturers who depend on welding in the fabrication of their products. SW-15 showed a 69% increase in acceptance and use, as compared with but 5% for all electrodes produced by the welding electrode industry from 1950 through 1952.

A few of the principal users of SW-15 are:

J. D. Adams
Allis-Chalmers
E. W. Bliss
Caterpillar
Chicago Bridge & Iron
Fruehauf Trailer
Graver Tank & Mfg. Co.
The Heil Company
Illinois Central Railroad
Ingersoll Rand
Iowa Iron & Steel
Kansas City Structural Steel
Link Belt Speeder
Omaha Steel
Pullman Standard Car
Santa Fe Railroad
Sheffield Steel
Trackson Company

Others are joining these every month to share the many benefits which the SW-15 electrode provides their welding operations.



SW-15 was developed as a universal type electrode which could be used for an extremely large number of welding applications. There is nothing else like it among mild steel welding electrodes!

It performed its first welding job in 1939, when it was taken into the field to weld refractory towers and sustaining structures usually welded with an E6010 electrode. It bettered the standard welding time by 50%! Its unusual ease of manipulation won over welding operators and management at once. New operators were readily trained with the SW-15. Its x-ray quality set new inspection records.

Two years later, SW-15 was welding on E6012 applications in the farm machinery and heavy road equipment fields. Excellent weld appearance, easy slag removal, little spatter, good bead with no undercut helped improve the products of these manufacturers.

Manufacturers found that due to SW-15's ease of manipulation, operators avoided slag inclusions, fish-eyes and voids, even in welds made in the field. This greatly improved the appearance of products it was used on and enhanced the reputation of the SW-15 electrode.

SW-15 is just one of the welding electrodes in the A. O. Smith line. There's an A. O. Smith electrode for every welding requirement:

MILD STEEL		HIGH TENSILE		LOW HYDROGEN		STAINLESS		STAINLESS	
AWS Class	A. O. Smith	AWS Class	A. O. Smith	AWS Class	A. O. Smith	AISI Class	A. O. Smith	AISI Class	A. O. Smith
E6010	SW-10	E7010	SW-75	E6016	SW-64	502	SW-151	310	SW-359
E6011	SW-14	E7010	SW-80	E6016	SW-65	505	SW-152	310	SW-169
E6012	SW-11	E7020	SW-76	E7016	SW-84	410	SW-153	310	SW-168
E6012	SW-17	E8013	SW-86	E8016	SW-81	430	SW-154	310	SW-368
E6013	SW-15	E9010	SW-95	E9016	SW-90	308	SW-162	316	SW-160
E6013	SW-16	E9013	SW-87	E9016	SW-89	308	SW-362	317	SW-161
E6020	SW-35	E10013	SW-88	E10016	SW-100	309	SW-166	318	SW-158
		E10020	SW-101	E12015	SW-120	309	SW-167	347	SW-157
				E12016	SW-103	310	SW-159	347	SW-357

THE ONE MILD STEEL ELECTRODE

no other can match!

...in ease of handling

SW-15 is easy for operators of varying skills to use in all positions. Its smoother arc, self-cleaning slag, lack of spatter and freedom from undercutting makes it easy for anyone to produce good looking, sound welds with the least effort!

...in quality of weldments

SW-15 welds have high-strength, high-yield, good ductility and consistent x-ray quality. This extends its use to welding low alloy, high tensile steels such as corten and manten, impossible for electrodes whose physicals can't compete with those found consistently in SW-15 welds.

...in appearance of welds

SW-15 bead appearance is excellent. Operators become more quality conscious, inclined to turn out better welding jobs when they can put in good looking welds so easily. Fine appearing welds aid in the ultimate sale of welded products, build acceptance not otherwise obtainable!

...in usefulness

SW-15 has an unparalleled history of success in many fields. Shipyards, during the war, cut their welding time as much as 50% by actual test and used millions of pounds of SW-15. Farm and construction equipment builders found its ease of application and high resistance to impact desirable, and became large users. Field erectors of vessels and structures adopted SW-15 after testing its speed, ease of application, and x-ray quality in weldments. Every year, more manufacturers turn to the SW-15 electrode to spur production and improve the quality of their welded products.

...in cutting costs

SW-15 is in a class by itself. Elimination of time-consuming slag removal, absence of spatter to be cleaned up, ease and speed of application by any operator, freedom from undercutting, excellent weld appearance that reduces inspection costs, and avoidance of weld repairs, all lower production costs directly. Then, the freedom from worry over possible field failures tops off these reasons for the tremendous use of SW-15 electrodes.

...in your plant

SW-15 has a use in your plant or shop. May we suggest that you write us or contact your local A. O. Smith Distributor for full details and a chance to test this remarkable mild steel electrode.

A.O. Smith way

A.O.Smith

CORPORATION

WELDING PRODUCTS DIVISION

Milwaukee 1, Wisconsin

INTERNATIONAL DIVISION: MILWAUKEE 1, WISCONSIN

A. O. Smith is your best single source for

A.C. AND D.C. WELDING MACHINES

with rated outputs from 180 to 1400 amps.

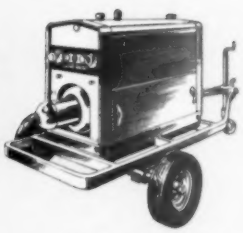
A.C. "CHAMPION"

A.C. "CHALLENGER"

A.C. "AUTOMATIC"

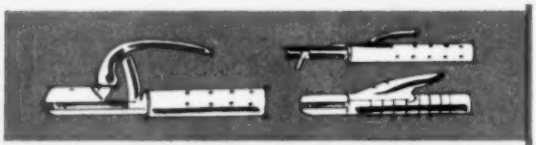
A.C. "UTILITY"

D.C. "RECTIFIER"



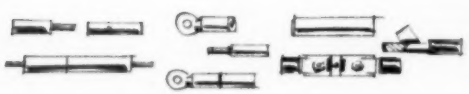
D.C. "FIELD KING"

WELDING SUPPLIES

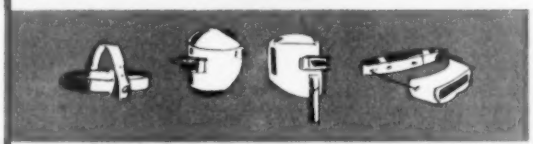


Electrode holders

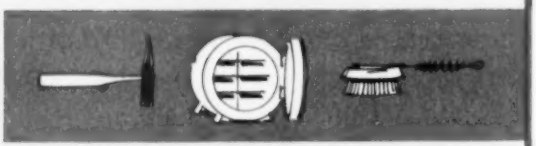
Ground clamps



Cable, connectors, lugs, and splicers



Helmets, shields, headgear, and goggles



Cleaning tools, Electrode drying ovens

Protective clothing and gloves

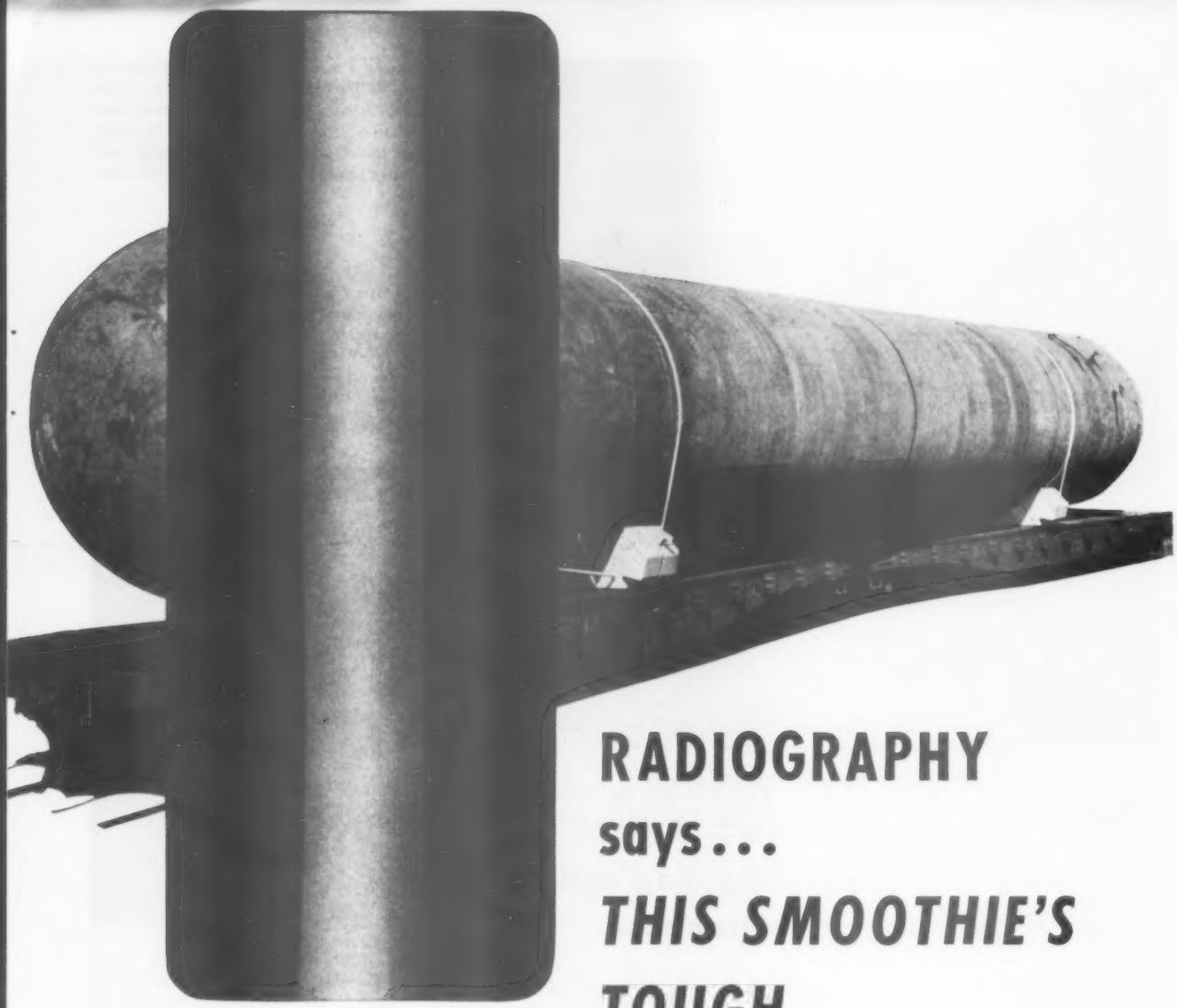
Simplify your welding buying with

ONE PURCHASE ORDER

A.O.Smith
CORPORATION

WELDING PRODUCTS DIVISION
MILWAUKEE 1, WISCONSIN

INTERNATIONAL DIVISION, MILWAUKEE 1, WISCONSIN



RADIOGRAPHY says... *THIS SMOOTHIE'S TOUGH*

This 90-ft. pressure vessel looks smooth, clean, and neat. But that's not all that welding did for it. It helped make the tank strong and tough with less weight and at lower cost.

Designers of pressure vessels can take full advantage of the welding process because each weld can be proved sound by radiography.

This is how radiography widens the opportunities for welders—why it can increase your business.

Wouldn't you like to know more about how it can help you? Get in touch with your x-ray dealer and talk it over.

EASTMAN KODAK COMPANY
X-ray Division, Rochester 4, New York

Radiography . . .
another important function of photography

Kodak
TRADE-MARK

Photo courtesy Sciaky Bros., Inc.

New BRUSH WELDING ANALYZER
simplifies troubleshooting and preventive maintenance

WITH the new Brush Welding Analyzer you can instantly spot improper calibration or inconsistent or faulty operation of three-phase resistance welders, single-phase resistance spot, projection, and seam welding machines. Chart records, which are immediately available, show the magnitude and timing of all welding variables, and indicate faulty firing of ignitron tubes or other difficulties.

In addition, the Brush Analyzer helps you make sure you meet specifications. Side by side on the chart are the two records of welding current and electrode force . . . showing contact gage settings, precompression force, weld force, rate of rise, forge force, and time of operation in milliseconds.

Brush Analyzers save you time in welding analysis, and in studies of AC or DC voltage or currents, strains, displacements, stresses, and other static or dynamic conditions. Brush representatives are located throughout the U.S., in Canada: A.C.Wickman Ltd.,Toronto. For bulletin write Brush Electronics Company, Dept. JJ-10 3405 Perkins Avenue, Cleveland 14, Ohio.

BRUSH ELECTRONICS COMPANY

INDUSTRIAL AND RESEARCH INSTRUMENTS
PIEZO-ELECTRIC MATERIALS • ACOUSTIC DEVICES
MAGNETIC RECORDING EQUIPMENT
ULTRASONIC EQUIPMENT

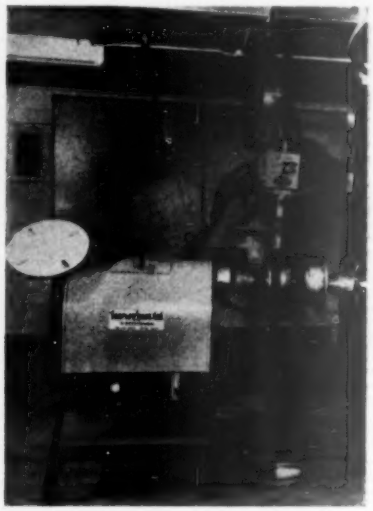
BRUSH ELECTRONICS

formerly
The Brush Development Co.
Brush Electronics Company
is an operating unit of
Clevite Corporation.

tained by writing to: Radio Frequency Co., 44 Park St., Medfield, Mass. The company also states that it will make application recommendations and quote guaranteed production rate if readers will submit prints and parts with relevant details.

Positioner

A new aid to difficult welding jobs has been put into production by Turn-A-Weld Products of Portland, Ore. Called the Turn-A-Weld Universal Positioner, this new product is designed to give greater speed for both automatic and manual welding of anything round.



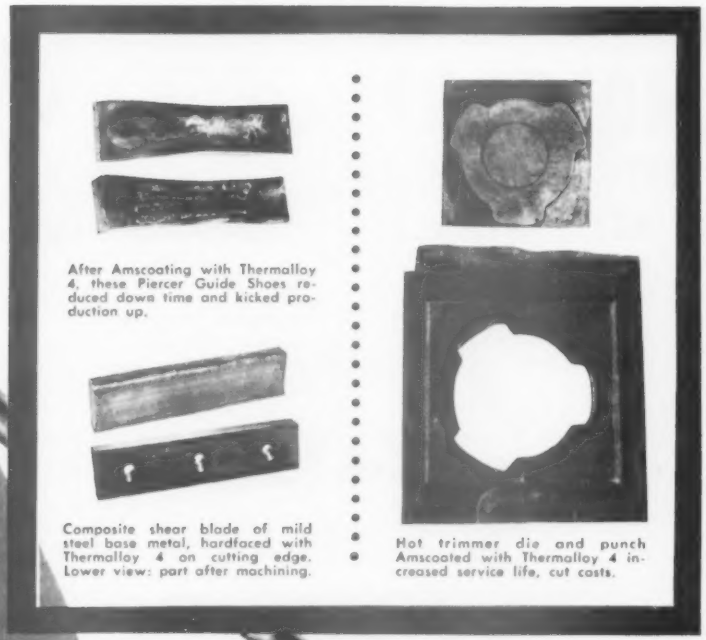
Adjustable to 40 speeds, a foot pedal controls the on-off switch. The Turn-A-Weld features a revolving table adjustable to 60 deg from center and an 8-in. lathe chuck for welding pipe, tubing, etc., of any desired length and an arbor with tapered cones for welding hubs, wheels, tractor rolls, etc.

The Universal Positioner is reported to cut welding time in half on jobs in tractor repair shops, manufacturing plants, pipe shops, welding shops, etc. The firm furnishes a 1-year warranty.

6-In. Clamp

A newly perfected and patented principle in clamping which permits extreme throat depth without sacrificing strength is currently available in a new 6-in. clamp with a $5^1/_2$-in. throat depth, according to William L. Maxson, President of Centinela Industrial Supply Co., 11930 Inglewood Ave., Hawthorne, Calif.

The new unit weighs but 4 lb and is produced from roll forged steel. It combines the best features of standard drop forged "C" clamp and a toolmaker's parallel clamp. By the use of a torque



After Amscoating with Thermalloy 4, these Piercer Guide Shoes reduced down time and kicked production up.

Composite shear blade of mild steel base metal, hardfaced with Thermalloy 4 on cutting edge. Lower view: part after machining.

Hot trimmer die and punch Amscoated with Thermalloy 4 increased service life, cut costs.

BEAT HEAT WEAR and IMPACT with AMSCO THERMALLOY 4

If you have a wear problem involving high temperatures and requiring resistance to impact, this new Amsco hardfacing electrode can save you time and money

Elevated temperatures are tough to cope with in situations involving thermal shock, metal-to-metal wear, hot gas corrosion, high temperature oxidation—especially when you need great impact resistance. Here's where Amsco Thermalloy 4 can step in and give you longer service life, and big savings in time and money.

Supplied as both a composite coated electrode for electric welding or as a bare cast rod for gas welding, both types deposit a high-carbon stainless alloy containing chromium and nickel as the principle alloying elements. In many applications, it can replace expensive nickel base alloy materials. Thermalloy 4 is packaged in 50-lb. boxes or in 10-lb. packages in 50-lb. boxes. For detailed analyses and other pertinent information, write for catalog sheet on Thermalloy 4 or contact your nearest Amsco distributor.

AMSCOATING

THE RIGHT WAY TO SAY HARDFACING

AMERICAN Brake Shoe COMPANY

AMERICAN MANGANESE STEEL DIVISION

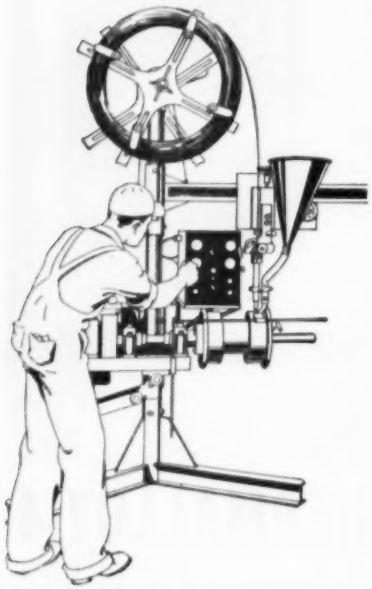
399 EAST 14th STREET · CHICAGO HEIGHTS, ILL.

Other Plants: New Castle, Del., Denver, Oakland, Cal., Los Angeles, St. Louis. In Canada: Joliette Steel Division, Joliette, Que.
Amsco Welding Products distributed in Canada by Canadian Liquid Air Co., Ltd.

THIS IS THE REXARC

MODEL RP4

AUTOMATIC WELDER AND POSITIONER



FOR REBUILDING ROLLERS, IDLERS AND FABRICATION WELDING

If you do custom welding or operate a fleet of crawler type tractors, shovels or draglines, the Rexarc RP4 is the machine for you!

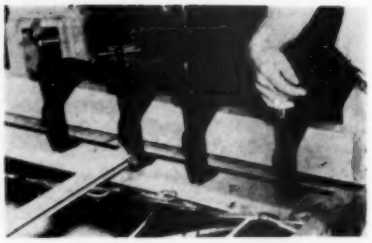
Here's automatic welding that is really automatic. Rexarc's exclusive electrical design gives you high frequency starting and stabilization of the arc. This assures fast, uniform deposit of the weld metal, minimizing stress and dilution of the parent metal. Saves time, gets work out faster.

Operation is simple and controlled from one convenient instrument panel. You can do longitudinal welding, too. Arrangement for automatic stepover, spiral or sequence welding is optional equipment.

See your Welding Supply Distributor or for complete details, write

THE SIGHT FEED GENERATOR COMPANY

57 EAST THIRD STREET
WEST ALEXANDRIA, OHIO, U. S. A.



converting, spring-bearing trunion in the new clamp, distorting, twisting action is completely eliminated. The trunion also absorbs vibration, thus insuring a firm, steady grip throughout milling, routing and shaping operations.

The spatter-free position of the screw has virtually eliminated the necessity for special shields or compounds in all welding operations.

The clamp is also available with regular throat sizes of 3, 6, 9 and 12 in.

Complete information may be had by writing Centinela Industrial Supply Co., 11930 Inglewood Ave., Hawthorne, Calif.

Soapstone Holder

Here is an unusual product of wide interest to workmen. It is the new See-Line soapstone holder. It is light weight, weighing only 3 oz . . . easy to see. . . *even through welders' goggles*, and it practically eliminates loss from soapstone breakage. For convenience, a strong, durable clip is attached. It also has an automatic locking device which makes the soapstone adjustable to any length for different marking conditions.

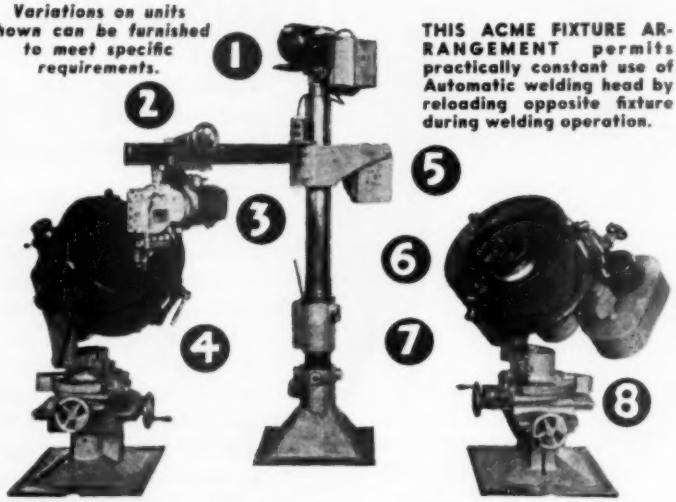
The See-Line soapstone holder was not originally designed for commercial production. It was perfected by Peter Martin, a design engineer, for his own personal use. He had found, as nearly all craftsmen engaged in metal marking have, that soapstone was difficult to handle . . . easily misplaced, and prone to breakage. *But*, soapstone was necessary to his job. So for his own convenience he designed a holder.

Wherever he used his holder, he found his fellow workmen were enthusiastic

ACME *Automatic* WELDING POSITIONER and FIXTURES

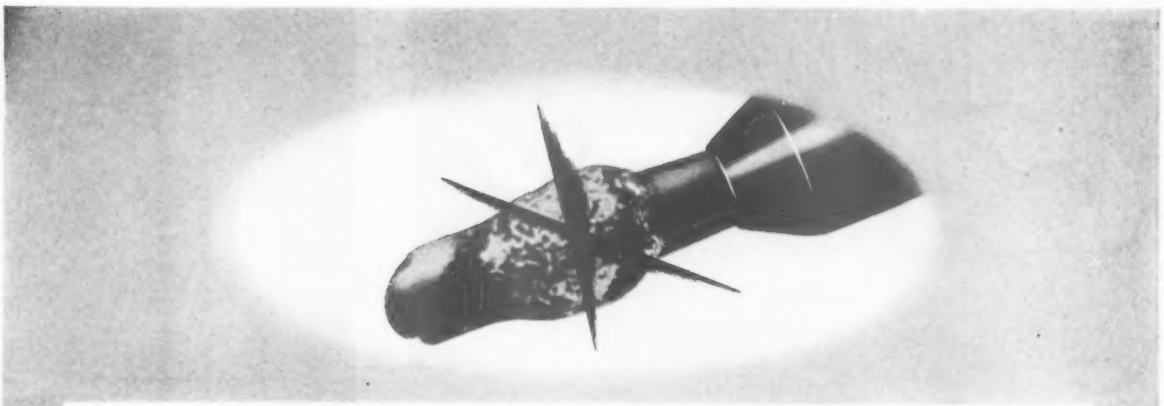
Variations on units shown can be furnished to meet specific requirements.

THIS ACME FIXTURE ARRANGEMENT permits practically constant use of Automatic welding head by reloading opposite fixture during welding operation.



1. Vertical power traverse with up and down inching button.
2. Hand wheel on carriage with locking arrangement for setting automatic welding head in proper relation to work.
3. Positioner arm swings and can be locked in proper position to either fixture.
4. 22" diameter wheel shown on fixture.
5. Fixture equipped with Reeves variable speed for proper feet per minute with reversing control. Fixture with two set speeds can also be furnished.
6. Holding machine can be set in either vertical or horizontal position. Shown in 45° angle.
7. Cross slide adjustment for fine setting so welding head on arm can be placed at either fixture without further adjustment.
8. Heavy copper brushes and holders mounted directly back of fixture for ground.

ACME *Manufacturing Co.*
1400 E. 9 MILE RD., DETROIT 20 (Ferndale) MICH.

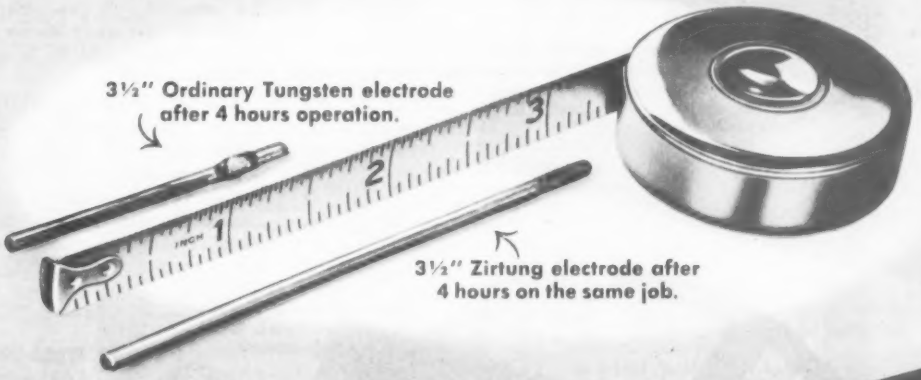


CROSS OUT CONTAMINATION AND SAVE WITH ZIRTUNG ELECTRODES

Work right ahead without interruption with Sylvania's new Zirtung electrodes!

This improved tungsten-zirconium rod really licks the contamination problem and lasts longer on any job. Saves time, saves electrodes and assures sound, dependable welds. Wonderful for mild steel, stainless steel, aluminum, and magnesium. If your nearest Sylvania Welding Distributor can't supply you, write today to: Sylvania Electric Products Inc., Dept. 3T-4910, 1740 Broadway, New York 19, New York.

An exclusive Sylvania development!



Here's why Zirtung saves:

1. **Greatly reduces electrode consumption.**
2. **Less contamination from material being welded.**
3. **Self cleans contamination without loss of electrode.**
4. **Greater arc stability.**

In Canada: Sylvania Electric (Canada) Ltd., University Tower Bldg., St. Catherine St. Montreal, P. Q.

SYLVANIA

LIGHTING • RADIO
ELECTRONICS
TELEVISION

lightweight fully insulated

CADDY

TYPE B Electrode Holders

Connected by the CADWELD PROCESS—
the greatest advancement in arc welding
in 20 years.

On Display
NATIONAL METAL SHOW
Oct. 19-23 Booth 1011

CADDY ARC WELDING
ACCESSORY DIVISION
ERICO PRODUCTS, Inc.
2070 E. 61st PLACE, CLEVELAND 3, OHIO

Soapstone Holder

about the new tool and wanted one for his own use. It was this continued popularity that made Martin decide that the See-Line should be produced on a commercial basis.

The See-Line adds another useful tool to the basic equipment of welders and others engaged in metal marking. The retail price is $1.49 complete with soapstone. It comes mounted on an attractive display card . . . one dozen to the card. It is manufactured by the See-Line Co., Inc., 618 Amarillo Bldg., Amarillo, Tex.

Spot Welder

The Taylor-Winfield Corp., Warren, Ohio, manufacturers of electric resistance welders, has introduced a new Type EB-3 Air-Operated Spot Welder.

The new Type EB-3 welder is a precision production tool, easy and convenient to operate and ruggedly constructed. Small and compact in form, it can easily be placed alone on a bench or in a bank or gang for production jobs.

Actual throat depth is 8 in. A welding force of either 750 or 1000 lb maximum is obtained by use of 80 psi line pressure. Electrode stroke is $1^3/_4$ in. maximum. The lower horn of the Type EB-3 welder is 2 in. in diameter and vertically adjustable to 4 in., plus an additional $1^1/_2$-in. vertical adjustment when the lower horn holder is inverted. Lower electrode holder

Dollar for Dollar... there's no better welder than... miller

New SELENIUM RECTIFIER TYPE DC Arc Welder 4 models

MODEL 100 Series Heavy Duty Industrial Arc Welder . . . 7 models

MODEL AEA-200L Engine driven A.C. Arc Welder and power plant . . . 3 models

WHETHER A.C. OR D.C. - THERE'S A WELDER FOR EVERY NEED!

- A.C. ARC WELDERS
- PORTABLE SPOT WELDERS
- HELIARC WELDERS
- WELDER AND POWER PLANT COMBINATIONS
- SR D.C. WELDERS

ARC WELDERS For Submerged Arc Welding 4 models

MILLER Welders are superior because into their design and manufacture go all of the new technical, electrical, mechanical and manufacturing developments, to make them the most efficient welders of their types. When you buy MILLER Welders, you may be sure your MILLER Welder is the latest of its type and ahead of others in its modern improvements . . . whether the welder is A.C. or D.C. . . . For example, MILLER SR Welders are the only new development in D.C. welding in the past 25 years. MILLER, the pioneer in electric welding equipment, continues to lead the industry in quality and new developments.

MODEL DEA-300L D.C. Engine Driven Rectifier Type Arc Welder

MODEL 99 A.C. 250 Amp. Arc Welder

MILLER WELDERS ARE SOLD ONLY THROUGH DISTRIBUTORS.

ARC · SPOT miller STANDARD WELDERS

miller

Write

Want to know full details of MILLER Welders? Write today . . . we will gladly send you complete information, or see your MILLER distributor.

ELECTRIC MANUFACTURING CO.

SINCE 1929 • APPLETON, WIS.

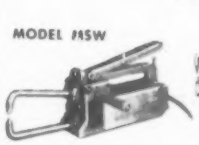
MODELS 61-F & 61-M General Utility Welders

MODEL BB Industrial ARC WELDER

MODEL 44 For The Small User

PORTABLE SPOT WELDERS

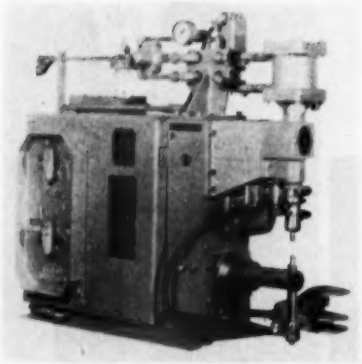
MODEL 1¼SW



MODEL 10KVA-20KVA

MODEL LMSW-52

A.C. ARC WELDERS For Heliarc Welding 9 models



(ejector type) is 6 in. long and 1 in. in diameter, with a No. 1 Morse Taper. Upper electrode holder (ejector type) is built as a permanent part of the upper terminal and is for a No. 1 Morse Taper electrode. Upper and lower electrodes are Code B-216, Domed Type, Class 2 Alloy with a No. 1 Morse Taper. Overall length is $1^1/_2$ in.

While many possible combinations of electrical controls are available, the use of synchronous-precision controls is recommended where exacting work requirements are essential. A magnetic or electronic contactor, as well as a sequence-weld timer, is necessary for the Type EB-3 welder.

A four-page folder on this welder concerning general construction, electrical controls and important operating and construction information is available.

Cleaning Tissues

The Carhoff Co. of 11706 Kinsman Road, Cleveland, Ohio, announces their NEW NO-FOG Cleaning Tissues. These tissues are the greatest discovery to date for cleaning and no-fogging Eye Glasses, Goggles, Glass in Welders' Head and Hand shields, Gas Masks, Windshields, Camera Lenses, Television Screens, and glass equipment in laboratories, etc. Works equally well on Glass or Plastic.

Safety Switch

The first NEMA XII safety switches—

a complete line of devices in special industry enclosures—have been announced by the Square D Co. This special industry switch has been introduced to meet the needs of the machine tool and processing industries as well as the industrial plants requiring a switch enclosure which will exclude dust, dirt, oil and coolants. The box is formed and welded from heavy code gage steel.

For details, write Square D Co., 6060 Rivard St., Detroit 11, Mich.

New Tempilstik°

Tempil° Corp. of 11 W. 25th St., New York 10, N. Y., announces the development of Tempilstiks°, Tempilaq° and Tempil° Pellets to indicate 525° F.

While Tempilstiks° (as well as Tempilaq° and Tempil° Pellets) for 500 and 550° F have been available for some time, as part of the standard series of Tempil° products, there has been a frequent demand for a Tempil° temperature-indicating material to signal the intermediate 525° F.

Tempilstiks°, Tempilaq° and Tempil° Pellets are now available in 12 or 13° intervals from 113 to 400° F and in 50 intervals from 400 to 2000° F, plus the newly developed 525° F rating. In addition Tempil° Pellets are also manufactured for the temperature range from 2050 to 2500° F, inclusive.

New Paint Stops Fire from Spreading

Everyone in the Welding Industry is studying with a great deal of interest the recent development of a new fire-retardant paint for the interior of their plants. It can be applied by brush or spray and actually stops fire from spreading. Everyone realizes the tremendous loss in life and property that can result from rapidly spreading flames, so this may well prove to be the greatest advancement in the prevention of these disasters.

Developed and Manufactured by Morris Paint & Varnish Co. of St. Louis, under license from the National Fire Retardant Corp.

"CONNECT WITH TWECO"

CABLE SPLICERS

A permanent splice to repair broken welding cables. Install with a wrench (or solder if desired) for a quick and efficient connection. Insulated with a tough fibre sleeve, 2 sizes for cable #6 through 4/0.

Use TWECO cable connections to improve your welding efficiency. Ask for TWECOLOG No. 8 with complete specifications and prices.

See Your Welding Supply Distributor

Tweco PRODUCTS COMPANY

TWECO PRODUCTS

BOSTON AT MOSLEY ST.
WICHITA, KANSAS

WELDING CONNECTORS

Saxe Welding Connection Units position and secure structural parts to be welded.

Clip K3A permits an adjustable connection.

These widely used units eliminate all hole punching, and, with welding, produce the most economical, safe, and quickly erected structural frame.

Write for 1951 edition, Structural Welding Practice Manual.

J. H. WILLIAMS & CO.
Buffalo 7, N. Y.

AIR REDUCTION CANADA, LTD.
Montreal 2, Canada

Buy "PROVEN FLUXES"
Years of GUARANTEED SATISFACTION
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"ANTI-BORAX" FLUXES

Insist on them — Unequalled Quality

No. 1 Cast Iron Welding Flux
No. 2 Brazing Flux for Brass, Bronze Steel, etc.
No. 4 Braz-Cast Flux for Bronze Welding Cast Iron
No. 5 & 8 Cast & Sheet Aluminum Fluxes
No. 9 Stainless Steel Welding Flux
No. 11 Tinning Compound
No. 16 Silver Solder Paste Flux

Mfg. By
ANTI-BORAX COMPOUND CO., INC.
Fort Wayne, Ind.

NEW LITERATURE

Welding of Nickel Alloy Steels

This Bulletin, A-93, contains 44 pages, with tables, graphs and illustrations and is a study of the various welding techniques, including some of the newer inert gas processes, electrodes, preheat treatments and postheat treatments. Typical results that can be expected are given for the low-alloy high-strength steels, low-carbon and medium-carbon engineering alloy steels and special steels. International Nickel Co., New York 5, N. Y.

Welding Stud

Shielded Stud Welding Co., 1540 Seabright Ave., Long Beach 13, Calif., has issued a new 4-page brochure giving features of their low-cost steel studs which are aluminum coated (patents pending) over their entire surface. Copy is available without charge upon request.

Rectifier-Type D-C Welder

Miller Electric Manufacturing Co., Appleton, Wis., 4-page folder describes the new SR line of welders, their magnetic amplifier principle, "pulsating direct current" principle and details of design and construction. Illustrated also are specialized applications—remote control, specialized control and parallel operation.

Maintenance Welding

A new 4-page folder containing a check list "to greater savings in Oil Industry Maintenance Welding" has been issued by Eutectic Welding Alloys Corp. of Flushing, N. Y.

The various salvage and maintenance applications are discussed, item by item, from drill bits to crown block to show precautions and procedures recommended for efficient welding operations in this industry.

"When correct techniques and materials are used," says Eutectic, "the service life of parts can be extended beyond even that of a replacement and lasting repairs can be effected at a speed hitherto unknown."

The new booklet, TIS 1706, may be obtained free upon request to Eutectic Welding Alloys Corp., Dept. "P," 40-40 172nd St., Flushing 58, N. Y.

Weld Standards

To enable the engineers of machinery manufacturers and fabricators to acquire both cost and quality control over welding operations, the Lincoln Electric Co. of Cleveland, Ohio, has created and is making available a set of Weld Standards. The Standards are available on tracing paper so that they can be blueprinted or otherwise reproduced for the purpose of giving engineers and production personnel a standard for communicating welding procedure on drawings. The Weld Standards are based on procedures developed by Lincoln and the weld symbols of the AMERICAN WELDING SOCIETY.

A common problem to engineers and designers in designing for welded machines or structures is assuring that the welds specified are made as specified. This is a problem in both communication and cost and quality control. The Lincoln Weld Standards help solve both aspects of the problem. The Weld Standards, with a simple letter designation, specify according to plate thickness both the type and size of weld as well as how it should be made. With both engineering and shop using the same standards, having prints of the standards available at all times, the engineer can be assured that the weld he has specified will produce the results for which it is designed. The shop can be assured that it is making the weld desired and according to specifications for economy, strength and appearance. When the information in the Weld Standards regarding plate preparation, fit-up, etc., is followed, the use of procedure for a specified plate thickness will result in a weld equal to the plate in section and strength. Weld performance is guaranteed by the use of Weld Standards.

For a specified plate thickness Weld Standards give the electrode type and size, the polarity and current, electrode melt-off rate, arc speed, number of passes or beads, feet of joint welded per hour and the pounds of electrode required per foot of weld. All of these factors are specified by using the Weld Standard designation in conjunction with the regular AMERICAN WELDING SOCIETY symbol. Standards have been prepared for manual welding of butt welds, fillet welds, lap and corner welds in all positions in both plate and sheet metal.

Complete sets of 19 sheets on tracing paper in a loose-leaf binder are available from the Lincoln Electric Co., Cleveland 17, Ohio, for $1.00 per set, postage prepaid in USA; $1.50 elsewhere.

Electrode-Selector Chart

A new four-page electrode-selector chart designed to present up-to-date information on the complete line of G-E welding electrodes has been announced as available from the General Electric Co., Schenectady 5, N. Y.

The chart, designated GEC-657E, contains condensed data on the company's line of mild-steel, low hydrogen-low alloy, low alloy-high tensile, hard-surfacing and stainless-steel electrodes.

Tabular material includes welded properties, metal deposit analysis, technique, type of specification requirements met, major competitive types, description and advantages, and application data for each type of electrode listed.

Data Card

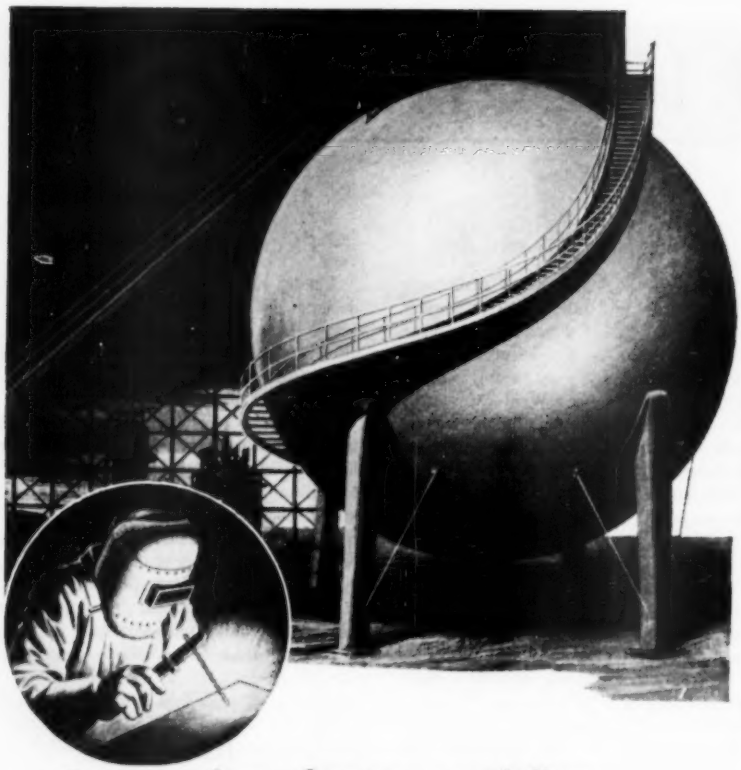
A new data card, designed to help individuals involved in the design and operation of tubular equipment at elevated temperatures and pressures, has been issued by the Tubular Products Division of the Babcock & Wilcox Co.

Known as TDC 154, the data card tabulates the maximum allowable stress values (S values) of a complete range of seamless and welded carbon, alloy and stainless steel tubing and pipe. An adaptation of Table P-7 of the new ASME Boiler Code, the data card is complete with notes and formulas for the calculation of the maximum allowable working pressures of tubing and pipe.

Copies of this helpful data card are available free upon request to the sales offices of the division at Beaver Falls, Pa.

S.S. *Ocean Vulcan* Sea Trails

The Admiralty Ship Welding Committee, in its study of the special problems arising from the use of welding in ships' structures, has carried out a program of investigations to compare the structural behavior of welded and riveted ships under the forces experienced at sea. For the reasons stated in this volume and in the light of the information obtained from the earlier still water structural tests, the Committee decided that the most promising approach to this problem would be to proceed with a series of trials in three stages, which may be briefly summarized as follows:



For ***easier, faster welding***
use one electrode for all positions

Gas or liquid retaining hortonspheres are welded in the field. In the process, every welding position is used—downhand, vertical and overhead. Today, because *one electrode* can produce sound weld metal from every position, considerable time is saved by eliminating frequent electrode changes.

You can profit from this same advantage by using Arcos Low Hydrogen Electrodes for welding high tensile steels. With few exceptions, once you select the proper electrode, you can keep on welding with *one electrode* —no need to change electrodes for different welding positions. Besides saving time, inventory can be simplified. Arcos quality controls, highest in the industry, assure you dependable, uniform weld metal for every application. Write for "The ABC's of Welding High Tensile Steels".

ARCOS GRADE	A. W. S. SPEC.
Tensilend 70	E7016
Tensilend 100	E10016
Tensilend 120	E12015
Manganend 1M	E9015
Manganend 2M	E10015
Nickend 2	E8015
Chromend 1M	E8015
Chromend 2M	E9015

ARCOS CORPORATION, 1500 South 50th Street, Philadelphia 43, Penna.

WELD WITH ARCOS
LOW HYDROGEN ELECTRODES

Investigation of the stress response of two sister ships, the welded *Ocean Vulcan* and the riveted *Clan Alpine* under various states of loading in still water.

Investigation of the loads imposed on one of these ships, the *Ocean Vulcan*, under prolonged periods of sea service.

Application of the results of the still water trials to the probable maximum loads derived from the sea trials, in order to estimate the probable maximum stresses at sea for both the welded and riveted ships.

The book consists of 175 pages copiously illustrated with diagrams and charts. Copy available at $8.00 postpaid. American Agents: British Information Services, 30 Rockefeller Plaza, New York 20, N. Y.

Taylor House Organ

Taylor Forge & Pipe Works, Inc., Chicago 90, Ill., have just launched a new House Organ designed to be of help to engineers and designers. This publication is available, upon request, to people interested in the problems dealing with piping and pressure vessels. The first issue contains some very interesting technical information on the Taylor Forge Products, examples of their use, and an important article entitled "Keeping Up to Date on Codes and Standards."

Structural Design with Aluminum

How to avoid beam failure in torsion and in compression is explained in Reynolds Metals *Technical Advisor* No. 22, just off the press. In this issue, Reynolds' Director of Application Engineering outlines how to most effectively design aluminum beams subjected to bending stresses.

Other feature articles in *Technical Advisor* No. 22 include information on a new procedure for keeping architectural aluminum bright when exposed to city and industrial atmospheres; and a description of a newly developed machine for automatically making circumferential welds in aluminum pipe lines. The new machine is so fast that 40-ft lengths of 6-in. aluminum pipe can be lined up, welded into the line and lowered into the ditch at an average rate of *1 every 3 min.*

The question-and-answer section in this issue covers such items as machining 638 aluminum, anodizing aluminum alloys, wet-blast chrome plating, extrusion mill finishes, materials for cable clamps, the "Koldweld" process, subzero air quenching, aluminum boat design.

Copies of *Technical Advisor* will be sent without charge upon request to Reynolds Metals Co., Desk PR, 2500 S. Third St., Louisville 1, Ky.

Solder and Brazing Alloys

A four-page illustrated bulletin describing its line of flux-containing electrical, electronic and mechanical bond paste solder and brazing alloys and covering the new two-part semiautomatic soldering technique is announced by Fusion Engineering, Cleveland, Ohio.

A chart is used to illustrate the melting ranges of each solder and brazing alloy type and is color-coded to indicate whether the paste alloy is designed for electrical or mechanical connection.

Line drawings of a variety of parts show the two-part Fusion soldering method which involves preapplication of paste to the part with separate heat application. This semiautomatic technique, possible only because the flux is contained in the paste solder alloy, eliminates much of the hand operation formerly required, greatly reducing labor costs.

An automatic paste applicator is also shown with a description of Fusion's engineering service available to mechanize production soldering operations. A laboratory kit of Fusion Alloys, complete with torch and dry pellet fuel, is also described. Bulletin FF-1 may be obtained by writing to Fusion Engineering, 4504 Superior Ave., Cleveland 3, Ohio.

Butt Welding Machines

A new brochure describing the semiautomatic butt-welding machines manufactured by Solar Aircraft Co. is now available from the company. Solar butt welders are special production tools for fabricating sheet metal into smooth cylinders and joining flat strips and sheets.

The butt welders are available in two standard sizes, one which welds seams up to 30 in. long, the other which welds seams up to 60 in. in length. Designed to obtain high output with semiskilled labor, the units weld a wide variety of metals and produce high-quality ductile weldments.

Copies of the butt-welder brochure may be obtained from the Contracts Division, Solar Aircraft Co., 2200 Pacific Highway, San Diego 12, Calif.

Elements of Heat Treatment

This book, by the late George M. Enos and William E. Fontaine, is a practical introduction to the heat treatment of metals and alloys—designed to provide a workable knowledge of how mechanical properties may be altered to suit particular situations.

The authors define the major heat-treatment processes—annealing, hardening, tempering and normalizing—and show how each process is used to get the metal characteristics needed for a specific job. Background information on the

When high operating pressures call for ***strong welds . . .***

Arcos stainless electrodes can deliver top-performing weld metal for high-pressure jobs—as well as others—because the specific qualities needed are "built in" every electrode. Arcos makes many electrodes for all kinds of jobs—each requiring a different balance of physical, chemical, or metallurgical properties. But, no matter how varied the requirements, Arcos electrodes will meet every one—consistently—yielding in the flash of an arc the highest grade weld metal available today.

For more information about specific applications of Arcos quality-controlled stainless electrodes, send for booklet, "What Electrode Would You Use?"

ARCOS CORPORATION, 1500 South 50th Street, Philadelphia 43, Pennsylvania

WELD WITH ARCOS STAINLESS ELECTRODES

properties of metals and alloys is presented in order to develop the reader's appreciation of the changes wrought by heat treatment. Casting, forging, rolling, welding, machining and other processes related directly or indirectly to heat treatment are briefly discussed. Unique, cartoon-type illustrations lend clarity and interest to the development.

John Wiley & Sons, Inc., 440 Fourth Ave., New York 16, N. Y. Price $5.00.

Alloy-Clad Steels

Advanced techniques for fabricating clad steels are outlined on a convenient wall-mount chart developed by Lukens Steel Co., Coatesville, Pa., world's leading producer of specialty steel plate, plate shapes, heads and clad steels.

The "Clad Fabrication Data" chart gives all basic information for arc-welding and flame-cutting clad steel plates. Of special interest to designing, engineering and shop personnel of steel fabricating companies, and useful to equipment design groups, it is intended to be hung in the shop or office for ready reference.

Copies of this chart can be obtained by writing Manager, Marketing Service, Lukens Steel Co., Coatesville, Pa.

the D-C Rectifier Welder with ARC-DRIVE CONTROL

. . . INSTANTANEOUS RESPONSE TO ARC-LOAD CHANGES
. . . INSTANTANEOUS RECOVERY
. . . REDUCED ARC BLOW
. . . COMPLETELY ADJUSTABLE BY OPERATOR

Westinghouse RA Welders with new positive arc-drive control now prevent shorting when used on "drag" welding applications. In addition, they allow complete penetration on root passes of vertical and overhead welds. Arc-drive control is obtained by adjusting the ratio of short-circuit current to welding current without changing open-circuit voltage. Actual amount of arc-drive current can be varied by the operator.

For information on this improved RA Welder or other Westinghouse Welding Equipment, write Westinghouse Electric Corporation, Welding Division, P. O. Box 868, Pittsburgh 30, Pennsylvania.

J-21607-A-2

YOU CAN BE SURE...IF IT'S Westinghouse

STEEL RIBS IN THE SKY

Streamlined and Spectacular

Foretelling Space Structures of Tomorrow

This dynamic new book combining theory and practice thrills you with its tour of living rigid frames.

The Author

Martin P. Kern, M. ASCE, *Consulting Engineer is a noted lecturer, leading authority and a pioneer in their design and construction.*

107 photos, 181 pages—8¼ x 11, 6 Foldouts, complete design data and drawings of unusual rigid frame structures with special articles by eminent authorities.

PARTIAL LIST OF CONTENTS

Ready Tables for Instant Design Spans 50 Ft. to 150 Ft. Vary, Hts.

Organizing a Design
Selection of type of Frame
Comparison—Welded and Riveted Design
A Buck Private in the Wacs
Shop Fabrication
Knees
Bases for Rigid Frames
Ties
Struts and Bracing
A Private Chat with Architects
Creative Design of Welded Plate Structures
Derivation of Basic Equations
Analysis of Multi-Span Frames
Model Analyses
Measured Stresses in Two-Hinged Arch

Says Col. Jack Singleton, Chief Eng. A.I.S.C. and author of the famous Manual of Structural Design " . . . . intensely interested in all phases of rigid frame design; much has been published, yet it remains for your book to coordinate under one cover, information of utmost interest. . . . It is the only book of its kind and as such of exceptional value to engineers and architects . ."

Copies available through

American Welding Society

33 West 39th St.
New York 18, N. Y.

Price. $4.50 plus postage for continental U. S. (We pay postage when check accompanies).

Price. $5.00 plus postage outside U. S.

Simplified Precision Welder Control

for easier maintenance and increased dependability

Class 8993 NEMA Type S2H Synchronous-Precision Controller available with 600 or 1200 frame contactor. Can be arranged either for side of machine or wall mounting.

DESIGN LEADERSHIP

APPLICATION: *for welding stainless steel, nonferrous metals, and other critical spot welding jobs requiring electronic synchronous-precision control. (Available with current wave shape control auxiliaries for aluminum or alloy metals.)*

DESIGN LEADERSHIP features

Easier Maintenance . . . greatly simplified circuits require fewer tubes and only one operating relay (for the air valve).

Increased Dependability . . . all components designed specifically for industrial welder service. Tube circuits are fail-safe and initiating contacts are in isolated 24 volt circuit which supplies power to the valve relay.

Consistent Accuracy . . . regulated DC voltage on tube control grids compensates for supply fluctuation or tube variations . . . assures timing accuracy.

Greater Accessibility . . . timer, heat control and firing panels designed for easy inspection and maintenance, with quick fasteners and plug connectors.

Write for BULLETIN 8993
or consult your nearby Square D Field Engineer

SQUARE D COMPANY

1903 • 50 YEARS OF DESIGN LEADERSHIP • 1953

Beryllium Copper

Ways in which beryllium copper can help engineers solve their design problems are illustrated by actual uses in a new booklet, *Applications Unlimited*, offered by the Beryllium Corp., Reading, Pa. A pioneer in the development of this unusual metal, the Beryllium Corp. maintains the most highly integrated facilities in the industry and is the world's leading producer of materials incorporating the element beryllium.

Applications of Beryleo beryllium copper illustrated and described in the booklet cover many different fields—aircraft, automotive, home appliances, business machines, instruments, metalworking machinery, resistance-welding equipment and electrical and electronic products—to name a few.

Copies of "Applications Unlimited" are available without cost from the Beryllium Corp., Reading, Pa.

AWS NATIONAL FALL MEETING

Week of October 19, 1953

Hotel Cleveland
Cleveland, Ohio

REDUCE SPOT-WELDING REJECTS WITH DICE MICROHM METER
(Model 151-S)

MIL-W-6158 AND MIL-W-6000

This instrument, first developed by Dice, is now widely used in industry to maintain quality control and reduce rejects in the spot-welding of aluminum . . . by checking the surfaces of sheets to see that they have been properly cleaned. The Dice Model 151-S Microhm Meter measures a resistance as low as 1/2 microhm in the low range to 750,000 microhms (in the highest range). A low resistance reading indicates that the metal to be welded is properly cleaned. The first Model 151 made by Dice is still in perfect working condition after almost five years of constant use. For free bulletin giving detailed information on how the Dice Microhm Meter can help you, write today.

J. W. Dice Co., Englewood 2, New Jersey

"Non-destructive Testing and Measuring Instruments"

no welding acrobatics!

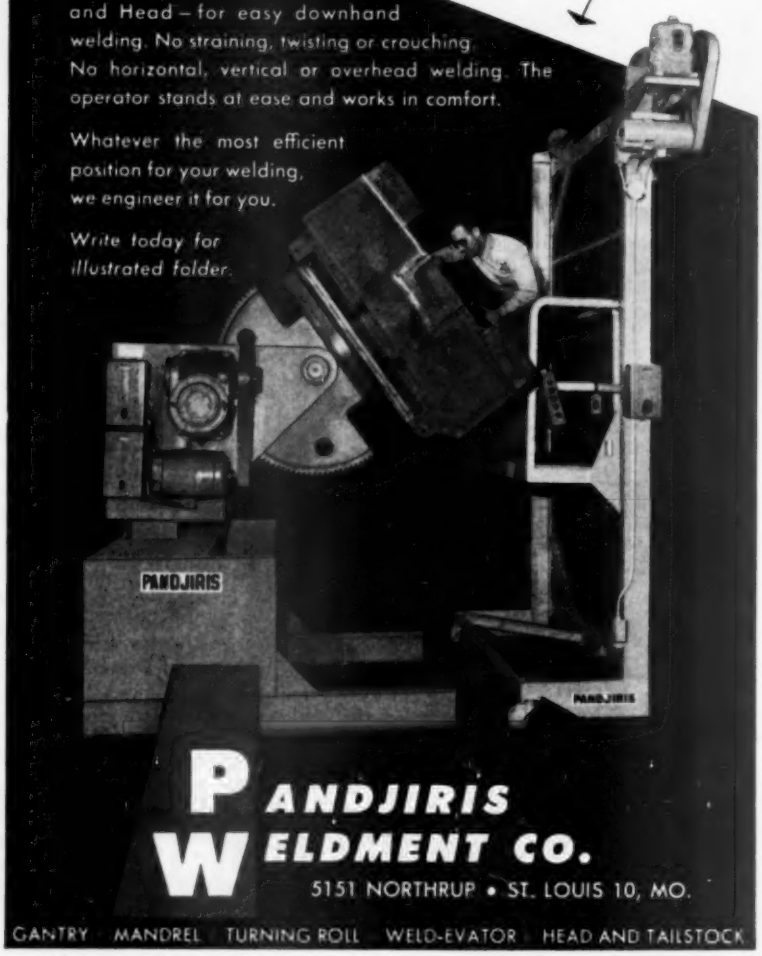
Awkward, fatiguing positions hinder production . . . waste man-hours. Help your operators weld more efficiently, with less effort . . . by using **Pandjiris "Weldmore" Engineered Positioning Equipment**

this is the right way!

Pandjiris positions "all three"—Man, Weld-Job and Head—for easy downhand welding. No straining, twisting or crouching. No horizontal, vertical or overhead welding. The operator stands at ease and works in comfort.

Whatever the most efficient position for your welding, we engineer it for you.

Write today for illustrated folder.

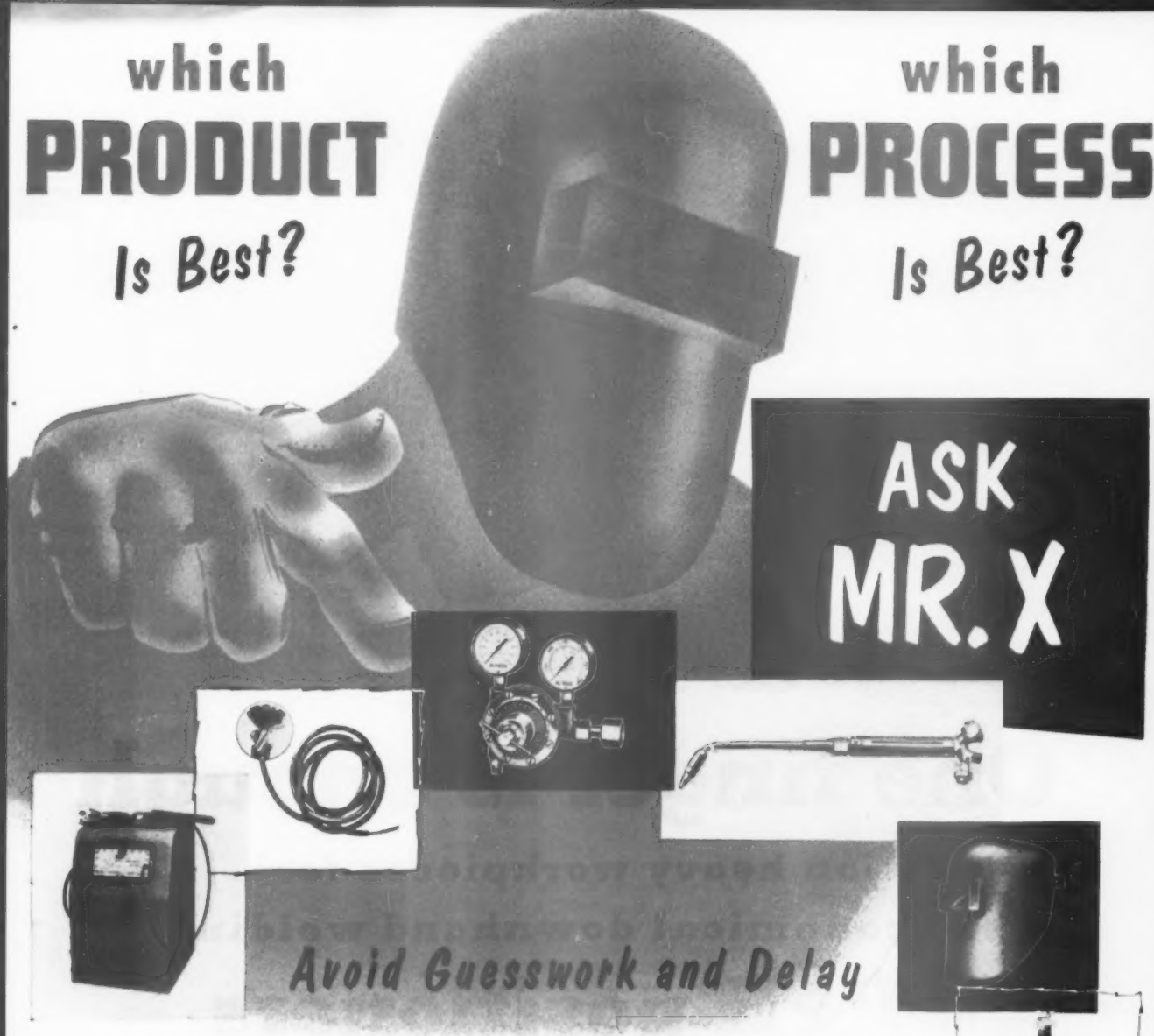


PANDJIRIS WELDMENT CO.
5151 NORTHRUP • ST. LOUIS 10, MO.

GANTRY MANDREL TURNING ROLL WELD-EVATOR HEAD AND TAILSTOCK

which PRODUCT Is Best?

which PROCESS Is Best?



Avoid Guesswork and Delay

Mr. "X" is not one individual—rather he represents the collective experience and "know-how" of Burdox welding men. This "know-how" is the result of years of working in close harmony with companies having all types of welding problems — using all types of equipment on all types of jobs. Mr. "X" is not partial to any particular welding process . . . he is an expert in all of them and therefore in a position to suggest the best possible products *for your particular needs*.

When you buy Burdox you not only get the finest welding and cutting equipment that money can buy, but you engage the services of our Mr. "X". He will work with your workers . . . increase your output and stretch your welding dollar!

Get acquainted with the complete Burdox line — write for FREE catalog.

BUY BURDOX top quality industrial gases

BUY BURDOX gas welding and cutting equipment

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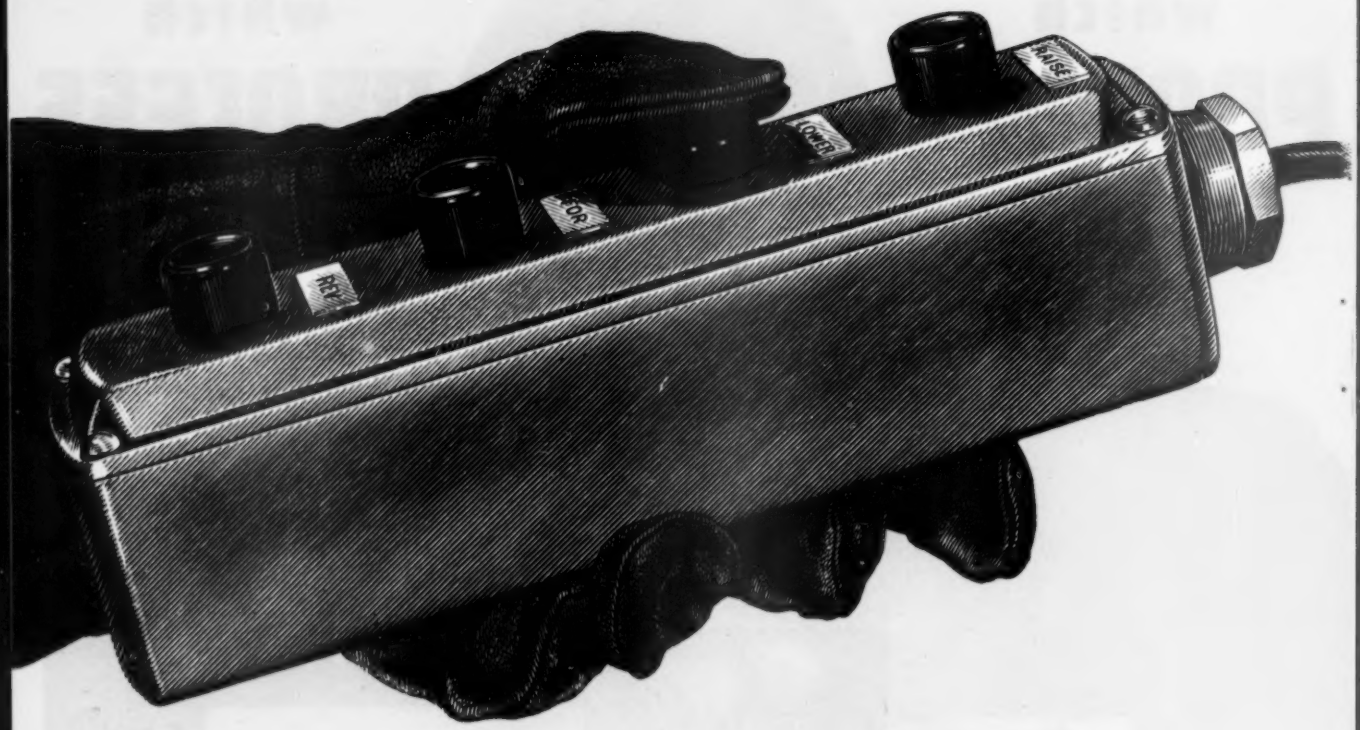

One finger is enough

to position heavy workpieces for fast, economical downhand welding

- with P&H positioners

Cut costs as much as 50% — position the weldment, not the operator! A touch of a control button does the work — saves handling time. Welding is downhand — permits using a larger rod for faster deposition. Welds are smooth, uniform. P&H Positioners are available in capacities from 2500 to 36,000 lbs. — remote-control and hand-operated models. Ask your P&H representative or distributor for further information — or write us.

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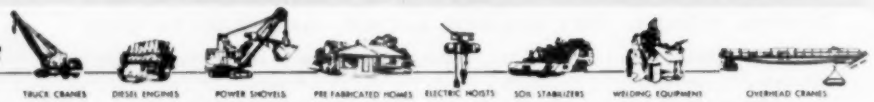
the P&H Line



TRUCK CRANES · DIESEL ENGINES · POWER SHOVELS · PRE FABRICATED HOMES · ELECTRIC HOISTS · SOIL STABILIZERS · WELDING EQUIPMENT · OVERHEAD CRANES

PERSONNEL

Sam Tour Heads Metallizing Committee

Sam Tour, general manager of Sam Tour & Co., Inc., a New York City industrial consulting, research and testing firm, has been elected as chairman of the Metallizing Committee of the AMERICAN WELDING SOCIETY.

This committee is active in preparing standards, compiling recommended practices, and conducting research programs on many aspects of metallizing of practical interest to industry. One important project currently being conducted by the Metallizing Committee is a research program of 12-years' duration to test the effectiveness of various types and thicknesses of metallized coatings in withstanding difficult exposure conditions. Members of the committee, who represent all segments of the metallizing industry, are personally contributing their skills, products, technical knowledge and research facilities to carry out this program.

In taking over the chairmanship of the Metallizing Committee, Mr. Tour, who has had 30 years' activity in metallurgy and metallurgical engineering, will retain his present post as chairman of the Sub-Committee on Metallizing for Corrosion Protection, which he has filled for the past two years.

Veteran G-E Leader Dies in Saugus

Henry O. Westendarp, 83, of 59 Main St., Saugus Center, Mass., retired General Electric executive and a former member of the School Committee, Finance Committee and Board of Health in Saugus, died suddenly at his home on August 10th.

A native of Roslindale, he was associated with the General Electric Co. for more than 40 years. Starting out in the old Thomson Laboratories, he spent his last 20 years with GE's Boston office as a sales engineer. He was a member of the Thompson Quarter Century Club.

During World War I he served on the Rationing Board in Saugus.

He was active in affairs of the First Parish Universalist Church of Saugus, holding various offices and was a charter member of the Boston Chapter, AMERICAN WELDING SOCIETY.

He leaves his wife, Mrs. Cora A. Westendarp; four sons, Henry O. Westendarp, Jr., a GE welding engineer in Schenectady; Bertrand D. Westendarp, a former member of the Saugus Finance Committee; Edwin M. Westendarp of Charrin Falls, Ohio, and Carl F. Westendarp of Lynnfield Center, and four grandchildren.

Employment Service Bulletin

Positions Vacant

Associate Professor for instruction and research in welding. Should have either M.S. or Ph.D. degree with established research record, particularly in the field of Resistance Welding. Will also teach some basic engineering courses. Either undergraduate or graduate degree should be in Industrial or Mechanical Engineering. Salary approximately $6500 for 9 months. Address applications to Chairman, Division of Mechanical Engineering, University of California, Berkeley, Calif.

V-297. Two or three experienced designers who have a good knowledge of welding techniques and welding design fundamentals. Excellent opportunity for for advancement.

V-298. Welding Engineer who has had experience in welding techniques including gas welding and cutting, manual electric arc welding, submerged arc welding and gaseous shield arc welding. Excellent opportunity for advancement.

V-299. General Welding Supervisor. Technically and practically qualified. Foundry experience desirable. Excellent opportunity with well-established mid-west organization. All replies confidential.

V-300. Sales Manager: If your experience includes selling machinery and equipment through distributors for the metal welding industry, a well known machinery manufacturer offers an unusually fine opportunity. Send full particulars covering education, experience, etc.

P&H

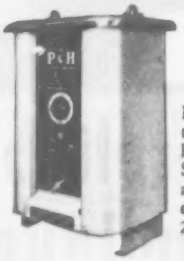
Arc Welders and Electrodes

put welding in your plant on a low-cost, high-production basis

P&H DC RECTIFIER WELDER

Has Dial-lectric Control for instantaneous heat selection at the work. Three sizes, 200, 300, and 500 amps., NEMA rated.

P&H WELDING TWINS



P&H AC WELDER

Has P&H Dial-lectric Instantaneous Remote Control. Sizes up to 625 amps., NEMA rated. Connectable to 220 and 440 volts.

P&H WN-301 Engine-Driven DC ARC WELDER

Portable. Equipped with Dial-lectric Control. Runs at only 1750 rpm. Welding service range, 60-375 amps., NEMA rated.

P&H LOW-HYDROGEN ELECTRODES

13 types that take the problem out of welding high-sulfur, free-machining, low-alloy, and high-strength steels; castings, etc.

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P&H WELDING DIVISION

HARNISCHFEGER CORPORATION

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Welding Arcs

Now—reliable, high-speed welding of aluminum and stainless steel*

New G-E Fillerarc Equipment makes a production reality of the consumable electrode gas-shielded welding process

Two years ago, General Electric welding engineers took on a challenging assignment: to develop equipment for consumable electrode gas-shielded welding that would be as dependable and trouble-free as conventional arc welding equipment.

Product of their success is the new G-E Fillerarc equipment which today makes available to American industry the full potential of this amazing process.

G-E Fillerarc equipment offers you:

VERY HIGH SPEED—wire speeds up to 750 in./min, highest ever on a production welder of this type.

WIDER USABILITY—far smaller wire can now be used, permitting welding of very thin sheets.

REAL DEPENDABILITY—unique design features minimize trouble, slash maintenance costs.

UNMATCHED RESULTS—current is automatically supplied for any wire speed, assuring high-quality work.

LOW PRODUCTION COSTS—due to very high speed, minimum operating trouble, increased flexibility.

EASE OF OPERATION—semi-automatic features make Fillerarc welding simple and easy to learn.

Additional information on this amazing new equipment is contained in these two bulletins:

GEA-6028 Fillerarc Welding Equipment
GER-819 Fillerarc Welding Process

They're available from your nearby G-E Welding Distributor. He's listed here and in the yellow pages of your phone book under "Welding Equipment—General Electric." Contact him today! General Electric Company, Schenectady 5, New York. 710-6

*Plus other metals including copper, nickel, and magnesium bronze.

See Fillerarc Demonstrated

at National Metal Show

Oct. 19th, Cleveland

GENERAL ELECTRIC

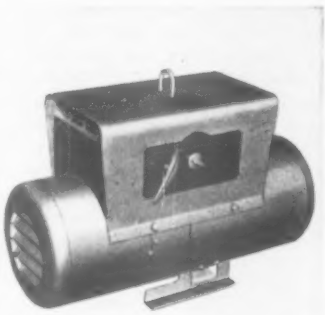
News

Information on equipment, electrodes and accessories from G.E., pioneer in arc welding

Radically new designs fulfill needs of Fillerarc process

NEW FILLERARC WELDER MINIMIZES STUBBING, BURN-BACK

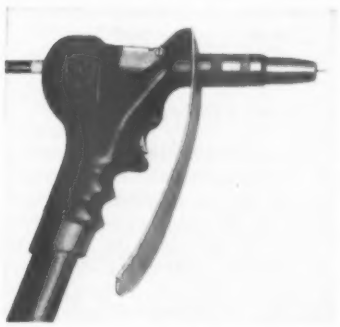
Here is the first self-regulating welder. Designed specifically for consumable electrode gas-shielded welding, it has a rising volt-ampere characteristic which automatically avoids burn-back and stubbing troubles, permits changing wire speed and current without adjusting the generator.



Other features:

- Self regulation—insures proper current for any wire speed.
- Low open-circuit voltage—makes accidental burn-back unlikely.
- Controlled current surge—for easy starts without scratching.

NEW FILLERARC GUN FEEDS SMALLER WIRE BY PULLING



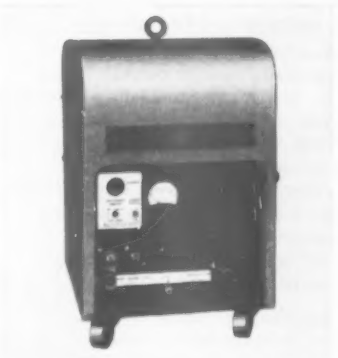
To overcome limitations on wire size caused by kinking, the Fillerarc gun is designed to **pull** wire into the gun. Result: the gun uses wire as small as .030 in., permitting the welding of far thinner sections than before.

Other features:

- Light in weight—50 ounces including normal unsupported cable.
- Simple trigger—for positive starts, automatic gas coverage.
- Easy threading—changing wire takes but a few minutes.

NEW FILLERARC WIRE DRIVE CONTROLS SPEED PRECISELY

To match other precision components, G-E engineers selected a G-E Thy-mo-trol† drive for accurately controlling wire feed. Remote control dial at work permits changing speed even while welding.



Other features:

- Dynamic braking—eliminates wire trimming before each start.
- Continuous wire-speed indication—no need to time and measure.
- Easily portable—weighs only 142 lb including wire, mounted on casters.

†**Reg.** Trade-mark of General Electric Company

Here's the name of your G-E Welding Distributor—

Alabama: Birmingham—Alabama Oxygen, Young & Vann Supply; Mobile—Turner Supply

Arizona: Phoenix—Consolidated Welding Supply

California: Fresno, Los Angeles, Oakland, Sacramento, San Diego, San Francisco, Ventura—Victor Equipment

Colorado: Boulder, Colorado Springs, Denver, Durango, Ft. Collins, Ft. Morgan, Greeley, LaJunta, Longmont, Pueblo—Hendrie & Bolthoff

Florida: Hollywood—Florida Gas & Chemical

Georgia: Atlanta, Macon—Welding Supply & Service; Augusta—Marks Oxygen; Columbus—Williams Welding Supplies

Idaho: Boise—Olson Manufacturing

Illinois: Chicago, Moline, Morton, Rockford—Machinery & Welder

Indiana: Evansville—Drill Master Supply; Ft. Wayne, Indianapolis—Sutton-Garten; South Bend—Perry Welding Sales & Service

Iowa: Des Moines—Machinery & Welder

Kansas: Coffeyville—Thompson Bros. Supply & Welding Equip.; Hutchinson—Kopper Supply

Kentucky: Louisville—Reliable Welding; Paducah—Henry A. Petter Supply

Louisiana: Alexandria, Shreveport—Hughes Oxygen; New Orleans—Consolidated Welding Supplies

Maryland: Baltimore—Arcway Equipment

Massachusetts: Boston—New England G-E Welding Sales Division

Michigan: Detroit—Welding Sales & Engineering; Grand Rapids—Miller Welding Supply

Minnesota: Duluth—W.P.&R.S. Mars; St. Paul—Production Materials

Mississippi: Jackson—Jackson Welding & Supply

Missouri: Kansas City—Hohenschild Welders Supply; St Louis—Machinery & Welder

Montana: Billings—Valley Welders Supply; Butte, Great Falls—Montana Hardware

Nebraska: Lincoln—Lincoln Welding & Supply; Omaha—Baum Iron

New Jersey: Kenilworth—Welding Sales Corp.

New Mexico: Albuquerque—Industrial Supply Co., Hobbs—Western Oxygen; Las Cruces, Silver City—Car Parts Depot, Inc.

New York: Buffalo—Welding Equipment Sales; New York—Welding Sales Corp.; Syracuse—Welding Engineering & Equip.

North Carolina: Charlotte—Dixie Gases; Gastonia—Gastonia Motor Parts

North Dakota: Bismarck, Fargo—Acme Welding Supply; Fargo—Dakota Electric Supply

Ohio: Akron, Cincinnati, Cleveland, Columbus, Dayton, Mansfield—Burdett Oxygen; Toledo—Odland Iron Works

Oklahoma: Tulsa—G-E Welding Sales Division

Oregon: Eugene, Portland—J. E. Haseltine; Medford, Portland—Industrial Air Products

Pennsylvania: Allentown, Philadelphia, Pittsburgh—Arcway Equipment

South Carolina: Columbia, Greenville—Welding Gas Products

South Dakota: Deadwood—Hendrie & Bolthoff

Tennessee: Chattanooga, Knoxville, Nashville—Welding Gas Products; Memphis—Delta Oxygen

Texas: Abilene—M&M Welding Supply; Alice, Corpus Christi—Crane Welding Supply; Alpine, El Paso, Marfa, Pecos—Car Parts Depot; Amarillo, Hereford—Tex-Air Gas; Brownsville, Harlingen—Acetylene Oxygen; Dallas—Hill Equipment & Supply; Houston—G-E Welding Sales Division; Lubbock—Welders Supply of Lubbock; Midland—West Texas Welders Supply; Odessa—Western Oxygen; Pecos—Welding Supply Co.; Plainview—Plains Welding Supply; San Angelo—Southwestern Welding Supply; Snyder—Western Welding Supply; Texarkana—Hughes Oxygen; Wichita Falls—Nortex Welding Supply

Utah: Salt Lake City—The Galigher Co.

Virginia: Richmond—Arcway Equipment

Washington: Seattle, Spokane—J. E. Haseltine; Spokane, Yakima—Industrial Air Products

West Virginia: Bluefield—Bluefield Supply; Charleston—Virginian Electric; Huntington, Logan—Logan Hardware & Supply

Wisconsin: Milwaukee—Machinery & Welder

Alaska: Anchorage—Northern Supply

Canada: Toronto—Canadian G.E.

Hawaii: Honolulu—American Factors, Ltd.

abstracts of CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 25, D. C.

2,646,649—Structure and Method for Cleaning Welding Rods, Eugene E. Rhodes, Dearborn, Mich., and Harry G. Scholtz, Wyandotte Mich., assignors to The Murray Corporation of America, Detroit, Mich., a corporation of Delaware.

This patent is on a special machine for removing adhered flux from the end of a welding rod. The apparatus includes a cylinder receiving a piston therein. Air is supplied to the working portion of the cylinder to aid in moving flux granules therethrough.

2,646,861—Welded Brake Beam, Arthus A. Weisberger, Chicago, Ill., assignor to American Steel Foundries, Chicago, Ill., a corporation of New Jersey.

A specialized welded type of a brake beam is disclosed in and covered by this patent.

2,646,995—Brazed Joint with Backing Ring, Roland J. Thompson, Washington, D. C.

This joint assembly includes spaced ends of cylindrical members and a cylindrical backing ring positioned in underlying relation to the ends of the members. A capillary concave opening is formed on the outside of the ring intermediate to it and the cylindrical members, and brazing material is present for flowing into this cavity when fused to seal the cylindrical members to the backing ring. The adjacent but spaced ends of the cylindrical members are beveled for receiving a welding material to bond the members and backing ring together.

2,647,483—Metal Forming and Welding Machine, William E. Price, Sixes, Org.

Price's patent relates to a machine having a follower engaging a feed screw of the machine and movable longitudinally of a movable jaw actuating unit provided in the machine. A stationary clamping jaw is mounted on supporting plates provided in the apparatus and means adjustably retain the stationary jaw on the supporting plates against movement away from the follower. A movable clamping jaw is supported on the supporting plates and has an opening therein in which the follower is received for displacing the movable jaw toward or away from the stationary jaw.

2,647,980—Platen Control Mechanism for Welding Machines, Charles D. Moore, Malden, Mass., assignor to Thomson Electric Welder Co., Lynn, Mass., a corporation of Massachusetts.

Moore's patent relates to a flash-butt-welder apparatus which has a movable platen, and platen controlling mechanism is provided including means for advancing the platen through an initial flash travel at a variable predetermined speed. Other means thereafter apply a variable predetermined force to the platen to cause it to advance further through an upset travel at a relatively faster speed and yet other means variable independently of the two first-named means are present in the machine for applying a still greater variable force to the platen after the platen has moved beyond its position at the initiation of the upset travel. Thus the final applied force may be varied independently of the upset travel speed.

2,647,981—Electrical Butt Seam Welding or Cutting Process and Apparatus for Carrying Out Said Process, Alfred Wogerbauer, Zipf, Oberosterreich, Austria.

A continuous electric butt-seam-welding process for producing tubes is disclosed in this patent. The method comprises providing two longitudinal parts capable of forming a tube when put together and such parts are forced toward each other by current conducting means until the parts contact so as to form at least two open seam gaps extending from the points of contact of the parts. Thereafter the parts are pressed together at the points of contact and welding current is applied to the parts, which current is caused to flow in a loop-like course around the seam gaps and through the points of contact.

2,647,982—Manufacture of Seam Welded Tubes, Robert M. Baker, Catonsville, Md., assignor to Westinghouse Electric Corp., East Pittsburgh, Pa., a corporation of Pennsylvania.

In this patent, means are provided that define a work passage through which skelp is moved longitudinally, which means comprise a longitudinal flux coil means and a transverse flux induction heating means. Such heating means include a core-structure having a pole face along the work passage, while the apparatus also has tube-forming roll means, an air-oxygen jet and welding roll means present therein.

2,648,748—Electric Arc Stud Welding, Charles Anthony Sayer, London, England, assignor to Cyc-Arc Ltd., London, England, a British company.

The method of this patent relates to a process wherein a stud and the plate to which it is to be welded are connected in the welding circuit and current of a limited value at full voltage is caused to flow between the stud and plate which are initially in contact. Next the stud is separated from the plate and current of limited value continues to flow to form an arc of low intensity. The current and the welding circuit is subsequently increased to full strength and, after a predetermined interval the stud is returned to the plate to effect the weld.

2,648,749—Electronic Sequence Control, Particularly for Travel Head Seam Welders, Joseph J. Riley and William S. Dustman, Warren, Ohio, assignors to The Taylor-Winfield Corp., Warren, Ohio, a corporation of Ohio.

This patent relates to a control circuit for controlling operations of electric-resistance welding apparatus of the type having electrodes and a weld contactor for controlling the flow of weld current to the electrodes.

2,648,750—Method of Making a Brake Beam, Loren L. Whitney, Hammond, Ind., assignor to American Steel Foundries, Chicago, Ill., a corporation of New Jersey.

Whitney's welding method relates to securing a brake-beam tension member to a brake head and compression member at least one of which defines an elongated cavity with an edge of the tension member. First a layer of weld metal is deposited in the cavity on the top of the edge of the tension member. Next the electrode is held stationary in the cavity for a period of time to form a puddle of weld metal, thereafter the circuit is broken, the electrode is moved to establish another electrical welding circuit to deposit another layer of weld metal on top of the first-mentioned layer. Next, yet another layer of weld metal is deposited on top of the first two layers by passing the electrode inboardly lengthwise of the cavity.

2,648,751—Transformer, Nelson O. Clark, Winthrop, Mass.

A special transformer is covered in this patent and it includes a single-turn secondary having divergent termini each of which are connected with a set of superimposed lower and upper clamping jaws. Electrode-supporting bars are revolably mounted in and are axially adjustable in their respective sets of jaws. Electrodes are adjustably positioned by the bars and

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It's the new, heavy-duty precision welding positioner with the 7 features you've always wanted in one unit

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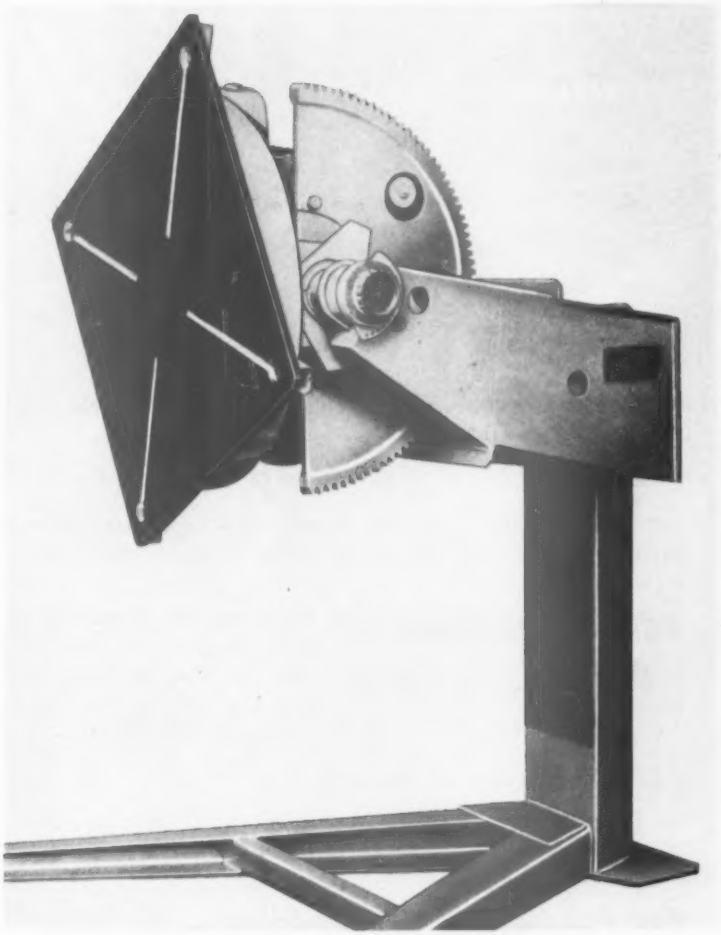

See these at the National Metal Exposition, 2111 Upper Exhibition Hall, Cleveland Public Auditorium, Cleveland, Ohio.

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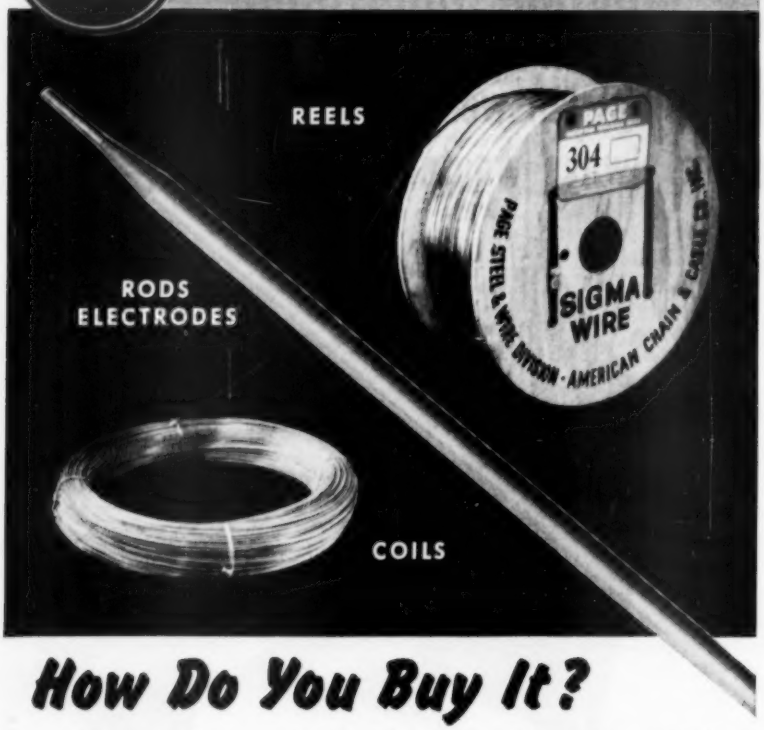

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PAGE Welding Electrodes Rods Wire

each electrode has an electric heating element. A work rest is adjustable positioned beneath the electrodes.

2,649,527—Butt Welding Tube Ends by Induction Heating, Edward Corbin Chapman and Roy E. Lorentz, Jr., Chattanooga, Tenn., assignors to Combustion Engineering, Inc., a corporation of Delaware.

This patent relates to apparatus for butt welding the ends of metal tubes together and has means for holding the tube ends in aligned but spaced relation. An induction-heating conductor of relatively narrow axial width encircles the extreme edge portions of the aligned tube ends and is adapted to create a narrow field of high-frequency alternating magnetic flux for heating only the end portions of the tubes. Baffle and other means are provided and are positioned in the tubes for feeding a nonoxidizing gas to the tube ends for flow from the tube ends through the gap initially separating the tubes. After the tube ends are heated to a desired temperature in the nonoxidizing atmosphere, other means press the heated tube ends together to perfect welding contact thereof.

2,649,528—Welding Ends of Copper Coils, Edward H. Koenig, New Haven, and John Chesnavich, Seymour, Conn., assignors to The New Haven Copper Co., Seymour, Conn., a corporation of Connecticut.

This patent relates to a method of positioning edges of copper sheets in juxtaposition over a groove in a carbon bar and a piece of copper filler wire is placed on top of the edges over the crevice in the bar. Next a tungsten electrode is placed over the filler wire and a direct-current welding voltage is applied across the gap between the tungsten electrode and the filled wire. A weld is started at one end piece and the tungsten electrode is moved along the filler wire to form a continuous weld along the length of the crevice. The weld is terminated at the other end piece, which welding action is performed while the arc is enveloped in a helium atmosphere.

2,649,529—Means for and Process of Heat-Treating Material to Be Welded Together, Harry B. Smith, Westfield, and George A. Dolan, East Orange, N. J.

This patent relates to a process of butt welding metallic members together and includes the steps of applying coil windings in relatively close relationship around adjacent joint parts and passing alternating current through the windings to induce electrical currents therein and provide quick preheating thereof. The next step is to move the coil windings quickly a distance apart and then the joint is arc welded between the members and when the welding is completed, the coils are quickly moved to a position to normalize the weld properly with a controlled current.

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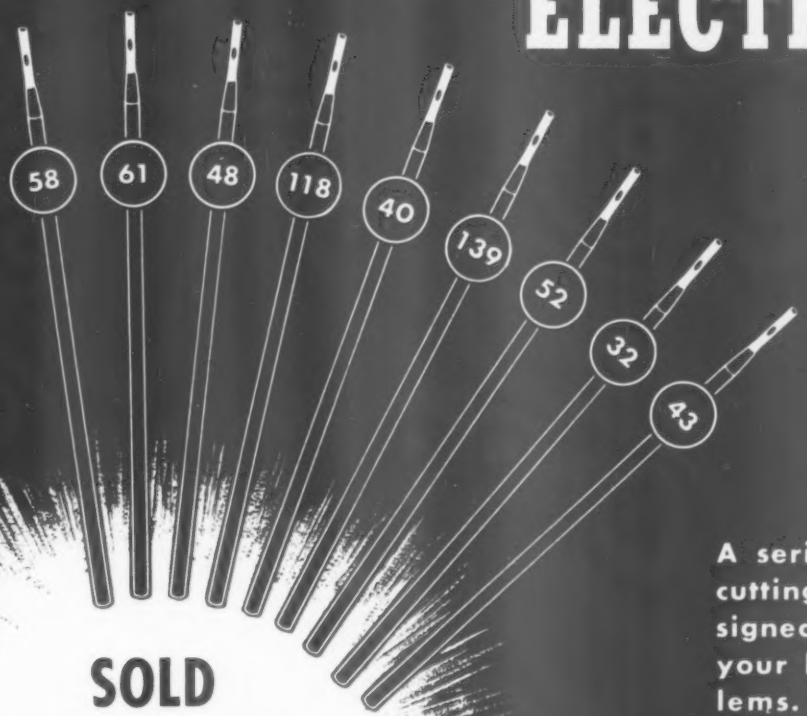

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This torch also shows the results of Rego's quality construction. Valve body of heavy forged brass. Tubes are of stainless steel in rugged triangular construction that really stands abuse. Head is of heat resistant stainless steel and resists scoring action of grit. Tip and mixer combined in one, therefore mixer is individually correct for each tip. Choice of 75° or 90° head.

OUTFIT NO. 71K

All-purpose, heavy duty industrial combination for welding and cutting. Includes GX Torch Handle, four GX tip assemblies, two Rego Two-stage regulators, easy-connect cutting attachment with cutting tip, hose, wrenches, lighter and goggles.

*T. M. of the B. B. Co. Chicago

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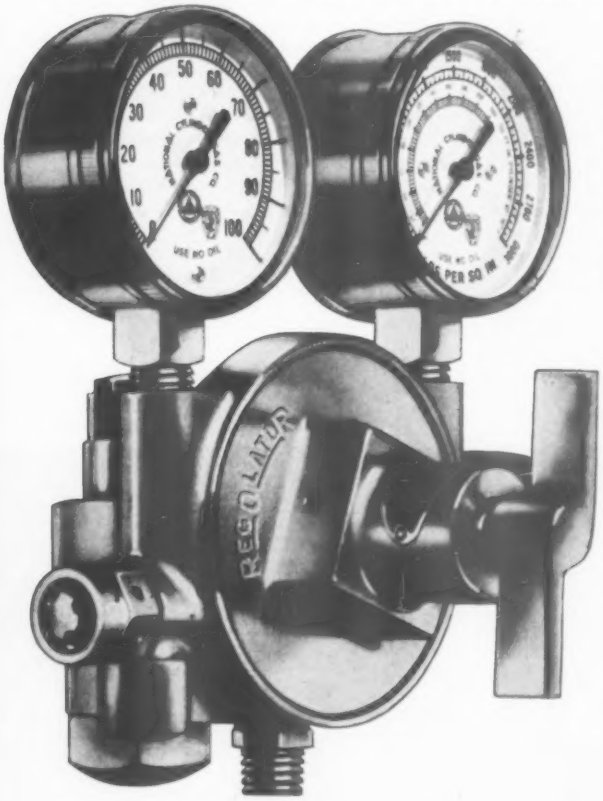
Thousands upon thousands of plants and shops the country over are using productive, profitable, time-and-money-saving Rego equipment in their oxy-acetylene welding and cutting operations. You will find this equipment superior in handling and operating characteristics, dependable and safe in use—factors that will reflect favorably in your production and cost records. And these factors explain why Rego has been so highly regarded in the industry for so many years. See Rego equipment at your nearest NCG branch or authorized NCG dealer. Or use the coupon below.



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NEWS OF THE INDUSTRY

Stainless Steel Research

The welding of stainless steel of the type designated as No. 347 is being studied at Rensselaer Polytechnic Institute by Dr. Ernest F. Nippes and associates in a research project sponsored by the Atomic Energy Commission's Schenectady Operations Office. Dr. Nippes is director of welding research at the Institute and a member of the faculty in metallurgical engineering.

The contract of the AEC with the Institute, reports Dr. Wendell F. Hess, director of Rensselaer research, calls for completion of this phase of the research project by July 1st of next year. The present contract is a renewal of one under which a year's research has already been made by Dr. Nippes and his associates in the investigation.

Welding Engineering Conference

A special conference on welding engineering will take place in Mannheim, Germany, October 15th to 17th under the auspices of Deutscher Verband für Schweisstechnik Ev., Mannheim, Viktoriastrabe 28, Fernsprech-Sammel-Nr. 4 18 28. The subjects to be discussed at the conference are "Shrinkages," "Shrinkage Stresses" and their effects on "Engineering Structures in Metal."

AISC Meeting

The American Institute of Steel Construction will hold its 31st annual convention this year, November 30th to December 4th, at the Boca Raton Hotel and Club, Boca Raton, Fla.

1954 IAA Convention

The 1954 convention of IAA will be held in Chicago on April 7th, 8th and 9th. Headquarters will be the Palmer House.

The 1954 meeting will be one of more than usual international flavor with the awarding of the Morehead Medal to Pierre J. Philippon, Director of L'Air Liquide, Paris, France.

Tube Turns, Inc., Branch Plant

Tube Turns, Inc., Louisville, Ky.—manufacturers of Tube-Turn welding fittings and flanges—has established a branch plant in Houston, Tex., to provide special service to the Southwest's petroleum industry. It makes possible immediate delivery of flanges bored and faced to the individual specifications of piping designers, as well as regular ASA flanges.

The plant also expedites delivery of other Tube-Turn products to the Southwest, since it serves as a rail and truck terminal, and provides a strategic break-up point for carload shipments made from Louisville.

The Houston plant, of modern design, is at 7120 Katy Highway. All operations are in charge of W. B. Whenthoff, Tube Turns' district manager. District offices, formerly in the Commerce Bldg., have been moved to the plant.

Photo Taken at Dinner Meeting International Institute of Welding, Copenhagen, Denmark, July 6, 1953

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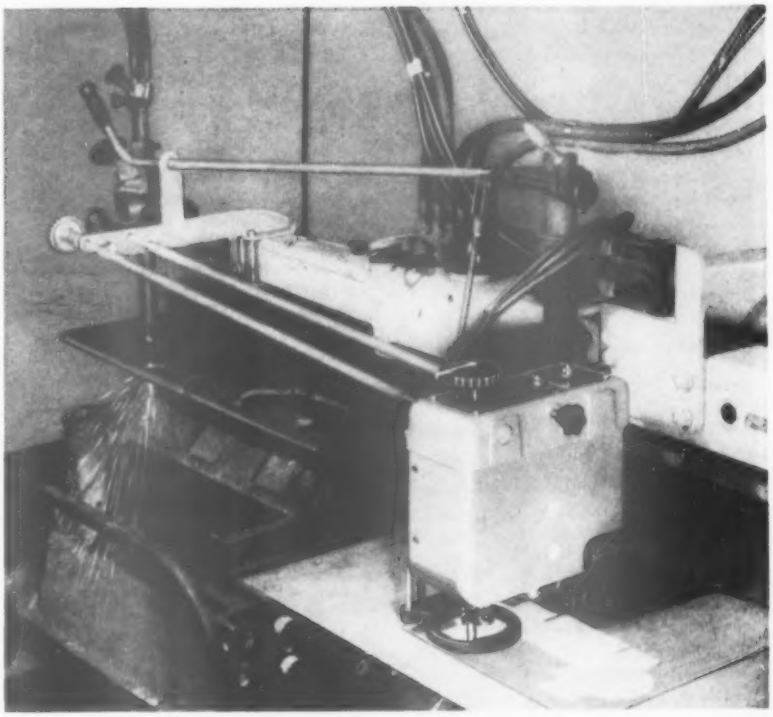

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The OXWELD Automatic Tracer follows practically any templet shape within extremely close tolerances. It can also be used to hand trace directly from blueprints or drawings. Drive action is smooth and steady with both single- and multiple-blowpipe setups.

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SECTION NEWS AND EVENTS

as relayed to C. M. O'Leary

Maryland

Baltimore, Md.—The *Maryland* Section announces the election and appointment of the following officers and committee chairmen.

Chairman—Harrison S. Sayre
First Vice-Chairman—L. H. Mathews
Secretary—James Forsyth
Treasurer—R. C. Bellas
Chairman, Membership Committee—C. H. Basal
Chairman, Program Committee—L. H. Mathews
Technical Representative—J. M. Phelps

Olean-Bradford Officers

Bradford, Pa.—The *Olean-Bradford* Section announces the election and appointment of the following officers and technical representative.

Chairman—M. T. O'Donnell, Bradford, Pa.
First Vice-Chairman—A. Lorenzini, Olean, N. Y.
Second Vice-Chairman—Guy Whipple, Bradford, Pa.
Secretary—Charles Smith, Olean, N. Y.
Treasurer—Bill Payne, St. Mary's, Pa.
Technical Representative—Gerald Beach, Olean, N. Y.

Election of Officers

Cincinnati, Ohio.—The following officers and committee chairmen have been elected and appointed by the *Cincinnati* Section.

Chairman—Harry C. Baker
First Vice-Chairman—Gerald E. Schauerte
Second Vice-Chairman—Chas. M. Allison
Secretary-Treasurer—Ross M. Burthwich
Chairman, Membership Committee—Chas. M. Allison
Chairman, Program Committee—Gerald Schauerte
Technical Representative—Wm. Maddox, Cincinnati Milling Machine Co.

Board Meeting

Cleveland, Ohio.—A large number of prominent Cleveland firms were represented at the recent Board Meeting of the *Cleveland* Section. It was announced that the following men are now actively operating in the various capacities to which they have been elected and appointed.

Chairman—Lew Gilbert, *Industry and Welding*
First Vice-Chairman—Robert J. Henry, Cleveland Diesel Div., GMC
Second Vice-Chairman—Allan P. Stern, President, Colonial Iron Works
Board of Directors—The above officers and William P. Mayer, Ernie H. Lauter, Clayton B. Herrick, Frank G. Flocke and Ray W. Metzger
Committee Chairmen—Technical Programs: Robert J. Henry
Symposium: Allan P. Stern
Membership: Robert H. Newton, The Robt. H. Newton Co.
Publications: Charles P. Berka, *Industry and Welding*
Publicity: Sanford Schwartz, Allied Advertising Agency
Special Events: Robert "Herb" Hinkel, General Electric
Properties: Harlan C. Smith, Williams & Co.
Reception: William J. Fauser, Scott-Tarbell Co.
Education & Plant Visit: C. T. Elder, Cleveland Electric Illuminating Co.
Coffee Program: Howard Schell, Burdett Oxygen Co.
Nominating: Harold P. Blum, Metal & Thermit Corp.
Tellers: Ross J. Yarrow, Republic Structural Iron Works
Advertising: Wm. O. Wolfendon, Hobart Welder Sales
House: Frank G. Flocke, The Thornton Co.

Mr. Gilbert outlined the plans for the coming year which include plant visits, technical programs, the Annual Symposium and several special events.

Western Michigan Officers

Grand Rapids, Mich.—The *Western Michigan* Section announces the election and appointment of the following officers and committee chairmen:

Chairman—Glenn G. Hickok, Ionia Manufacturing Co., Ionia, Mich.
First Vice-Chairman—Paul W. Wagner
Second Vice-Chairman—Douglas Harvey
Secretary—Gordon L. Hill, Aladdin Rod Mfg. Co., Grand Rapids, Mich.
Treasurer—Chester H. Voorhorst, Purity Cylinder Gases, Grand Rapids, Mich.
Chairman, Membership Committee—Edmund J. Chester
Chairman, Program Committee—Paul Wagner
Technical Representative—L. J. Harris, 400 Van Raolte Ave., Holland, Mich.

Directors Meeting

Houston, Tex.—Officers and Directors of the *Houston* Section met on July 29th and August 26th to discuss a program for the coming year for both the Houston Section and the newly formed Austin Division of the Houston Section.

It was decided that the meetings would be held on the last Wednesday and Friday, respectively, of each month. Austin members were urged to attend meetings in Houston, but a tentative program of speakers was outlined for Friday meetings in Austin. As many Houston members as possible were encouraged to attend these Austin Division meetings. P. V. Pennypacker and Ed Garland, both of Austin, have supplied the spark in organizing the Austin Division of the Houston Section. We would like to take this opportunity to welcome these new AWS members into the Houston Section.

Mr. Reed of the McKay Corp. is scheduled to speak on "Electrode Coatings" at dinner meetings in Houston on October 28th and in Austin on October 29th.

Philadelphia Officers

Philadelphia, Pa.—The following officers and committee chairmen have been elected and appointed by the *Philadelphia* Section:

Chairman—R. D. Bradway, New York Shipbuilding Corp., Camden, N. J.
First Vice-Chairman—R. D. Thomas, Jr., Arcos Corp.
Secretary—A. John Erlacher, United Engineers & Constructors, Inc.
Recording Secretary—Henry A. Young, K. Wm. Ostrom & Co.
Treasurer—A. M. Garcia, Morris Wheeler & Co.
Chairman, Membership Committee—L. D. T. Berg, Areway Equipment Co.
Technical Representative—Byron Gates, The Budd Co.

Portland Officers

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Chairman—Stanton Richardson

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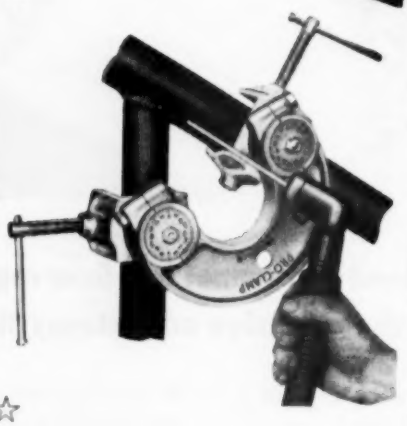

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Secretary—Fred M. Parker
Treasurer—George A. Conner
Chairman, Program Committee—B. Nealley Wood
Technical Representative—Harry Czyzeski

Salt Lake City Officers

Salt Lake City, Utah.—The following officers and committee chairmen have been elected and appointed by the *Salt Lake City* Section.

Chairman—Fred S. Thomas
First Vice-Chairman—Paul W. Ownby
Second Vice-Chairman—Frank Hulbert
Secretary—Darwin Christofferson
Treasurer—Ray L. Potts
Chairman, Membership Committee—A. B. Blakemore, Jr.
Chairman, Program Committee—Frank Hulbert
Technical Representative—Lynn J. Christensen

Boiler and Pressure Vessel Laws

Sheffield, Ala.—A number of prominent local city officers and industry representatives were present at the July 30th dinner meeting of the *Tri-Cities* Section held at the Muscle Shoals Hotel.

R. Milligan, AWS, Superintendent of the Boiler Division of the Ocean Accident and Guarantee Corp., gave an impressive talk, accompanied with slides, on Boiler and Pressure Vessel Laws, Codes and Welding Qualifications. Some of the slides gave visual proof of the tremendous damage caused by boiler explosions. The potential loss of life and destruction possible from a defective boiler caused much interest among those in attendance, particularly since the Tri-Cities area does not have a boiler code law.

Mr. Milligan's talk brought out many of the requirements of the ASME Boiler Code. After the talk, Chairman York conducted a lively discussion period concerning the need of and requirements for a local city ordinance requiring boiler inspection. Because of the interest shown by the city official, Mr. York offered his services to any interested parties to further explain the Boiler Code requirements.

AWS NATIONAL FALL MEETING

October 19—23, 1953

Hotel Cleveland
Cleveland, Ohio

LIST OF NEW MEMBERS

Effective August 1, 1953

DALLAS

Rath, Howard Whitley (C)

DETROIT

Hofer, Gust H. (A)
Myles, Richard G. (C)
Phillips, Kenneth (B)
Sharp, John F. (C)
Senn, Charles (B)
Tetens, Raymond E. (B)

EAST TEXAS

Arp, Forrest F. (C)
Mercer, Robert E. (C)

HOUSTON

Blasdell, William C. (B)
Bowling, Leonard C. (B)
Campbell, Dan W. (B)
Campbell, W. H. (B)
Cook, Albert B., Jr. (B)
Dobraski, William J. (C)
Farris, Wesley (B)
Garland, Ed. C. (B)
Garwood, G. L. (C)
Green, Roy C. (B)
Holmes, T. E. (B)
James, W. L. (C)
Jones, Otis E. (C)
King, E. T. (B)
LaFonte, Bob (B)
McLemore, F. D. (B)
Menefee, Lee (B)
Mullenix, Fred H. (C)
Ramsey, J. J. (B)
Rauch, John R. (B)
Repp, R. C. (C)
Snead, E. B. (B)
Spillman, H. J. (B)
Valentine, S. F. (B)
Vaughn, J. N. (C)
Wells, C. M. (B)
Wells, J. L. (C)
Werland, E. R. (C)
Wheeler, Rowland Sibley James (B)

IOWA-ILLINOIS

Custer, Robert L. (B)

LOS ANGELES

Oliver, Stuart K. (C)

MICHIANA

Ostrom, Ray (B)
Shimkus, Robert P. (C)

NEW YORK

Cole, Philip B. (B)
Craige, T. L. (B)

NORTHWESTERN PENNSYLVANIA

Perry, Jess B., Jr. (C)

PHILADELPHIA

Fernley, Robert C. (B)
Surma, Henry F. (C)
Von Lister, Lowell (C)

PITTSBURGH

Goldberg, Lewis (B)
Typek, Carl (B)

ST. LOUIS

Huitt, J. Floyd, Jr. (B)

SAN FRANCISCO

Fagan, James G. (C)
Hagerman, Douglas W., Jr. (C)

SUSQUEHANNA VALLEY

Mangle, Richard H. (B)

TRI-CITIES

Burke, John (C)
Eckert, Fred H. (C)

TUCSON

Henry, Charles Logan (C)

WASHINGTON

Cook, E. Lewis (C)
Dawson, Clifford H. (C)

WESTERN MASSACHUSETTS

Lawlor, Maurice S., Jr. (C)

WICHITA

Paxhia, A. L. (C)

NOT IN SECTIONS

Chadwick, George S., Jr. (B)
Forest, Hargett Earl (B)
Hibler, Russell L. (B)
Mikami, Hiroshi (B)
Myburgh, J. C. F. (B)
Pegram, Myron Hicks (B)

Members Reclassified

During the month of August

CHICAGO

Becker, R. Condit (C to B)

HOUSTON

Van Dresser, M. J. (C to B)

PHILADELPHIA

Phair, Robert S. (C to B)
Pfeffer, Harry M. (C to B)

PITTSBURGH

Priest, H. Malcolm (B to E)

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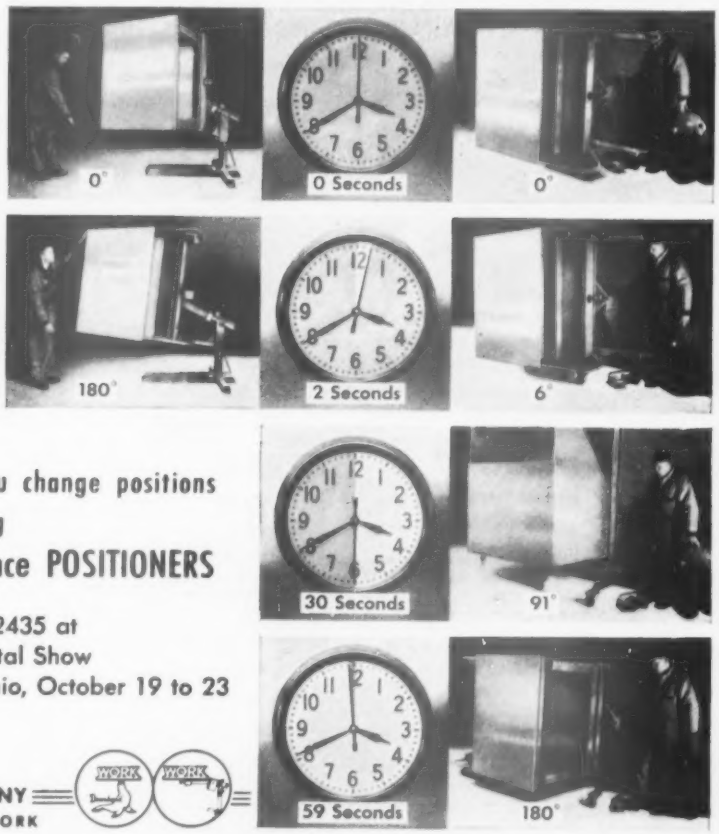

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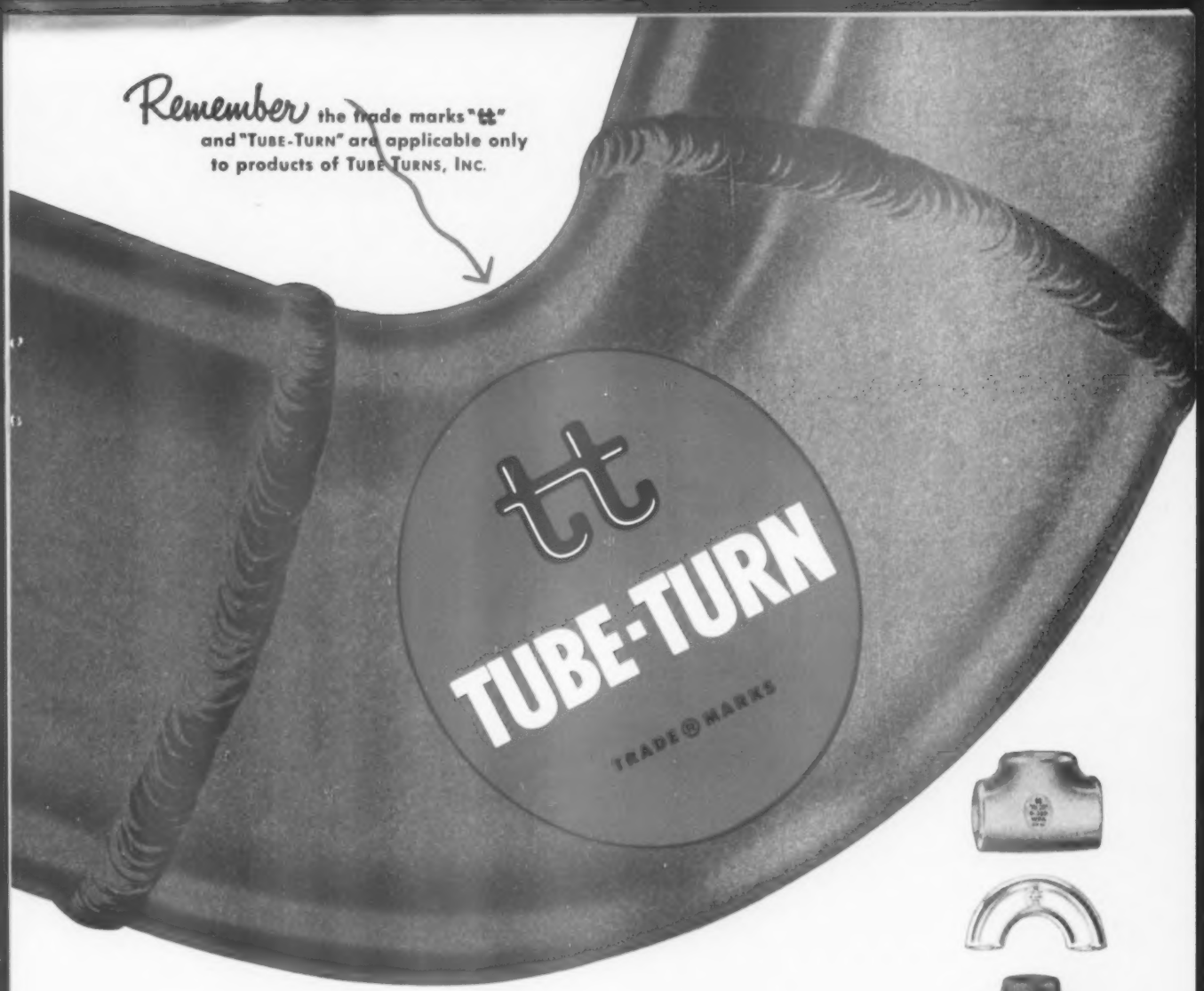

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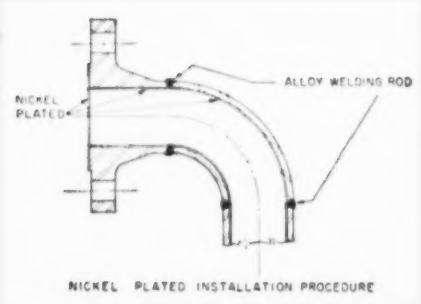

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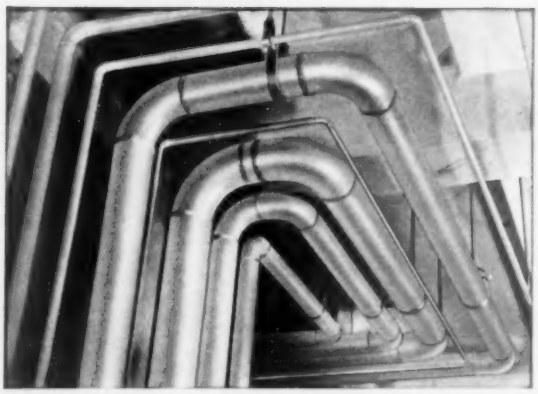

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Supplement to the Welding Journal, October 1953

The Effect of Alloying Elements on Welds in Titanium

The effects of iron, manganese, chromium and molybdenum on the mechanical and metallurgical properties of welded joints in titanium

by G. E. Faulkner, G. B. Grable and C. B. Voldrich

Abstract

Iron, manganese, chromium and molybdenum are the principal beta-stabilizing elements used as strengtheners in commercial titanium alloys. The effects of these elements on the mechanical and metallurgical properties of welded joints in titanium were studied in four series of experimental binary alloys prepared in compositions designed to cover the range where most of the useful alloys are expected to fall.

Inert-gas-shielded tungsten-arc-welded joints were made in $^1/_8$-, $^1/_4$- and $^1/_2$-in.-thick plates of the experimental alloys. These welded joints were studied for tensile, bend and notch-toughness properties, hardness and microstructure.

The bend ductility of welded joints in the alloys with lowest alloy content was fairly good. However, the bend ductility decreased rapidly with increasing alloy content and became low when 3 to 6% alloy content was reached. Postweld heat treatment was successful in improving the bend ductility of most of the welded joints in alloys containing up to 6% alloying element. With one exception, welded joints in plates containing over 6% alloying element had low bend ductility in both the as-welded and heat-treated conditions. The exception was the 13% chromium metastable-beta alloy which had excellent bend ductility in the as-welded condition. The trend in the notch-toughness properties of the welded joints with increasing alloy content was similar to the trend observed in the bend properties. The welded joints which had fair bend ductility generally had relatively good notch toughness, and those which had low bend ductility generally had low notch toughness. Transformation hardening of the weld metals and heat-affected zones appeared to be correlated with bend ductility and notch toughness in alloys containing up to 6% alloying element. Results indicate that factors such as freezing segregation, cast structure and contamination may have contributed to the embrittlement of the welded joints in the higher alloys. Freezing segregation was observed in the weld metals of most of the metastable-beta alloys.

G. E. Faulkner is Welding Engineer, **G. B. Grable** is Assistant Supervisor, and **C. B. Voldrich** is Chief Welding Engineer with the Battelle Memorial Institute.

Paper covers material presented at the AWS Thirty-third National Fall Meeting, Philadelphia, Pa., week of October 19, 1952 and at the AWS National Spring Meeting held in Houston, Texas, June 16–19, 1953.

INTRODUCTION

THE relatively light weight and high strength of titanium alloys have stimulated great interest in their possible use in certain types of ordnance equipment. However, the alloys have to be fabricated by welding to be of greatest value for most applications, and the weldments must have suitable strength, ductility and resistance to impact loading for use under service conditions.

Early attempts at welding the higher strength types of alloy showed that the heating and cooling cycles involved in the welding operation generally produced welded joints with low ductility. The majority of these alloys contain beta-stabilizing elements which strengthen the metal by stabilizing the relatively high-strength beta phase of the alloy.

Although considerable work had been done in determining the metallurgical and mechanical properties of titanium alloys containing beta-stabilizing additions, knowledge concerning the effects of these elements on

weld-joint properties was very limited. In order to obtain a better understanding of these effects as a basis for an approach to fabricating high-strength lightweight titanium alloys for ordnance applications, Watertown Arsenal authorized this investigation.

The objective of the investigation was to obtain fundamental information concerning the effects of iron-, manganese-, chromium-, and molybdenum-alloy additions on welded joints in titanium. These elements are the principal beta-stabilizing elements now being used in commercial alloys. In conducting the investigation, four series of experimental binary alloys containing the above elements were selected and prepared. The compositions of the alloys in each series were selected to cover a range in which useful alloys were expected to fall. Inert-gas-shielded tungsten-arc welds were made in 1/2-, 1/4- and 1/8-in. plates of the alloys, and the welded joints were investigated to determine their mechanical and metallurgical properties.

MATERIALS

The compositions of the four series of alloys prepared for this study are shown in Table 1. Commercially pure titanium metal was prepared also for comparison purposes. The following designations were given to the alloys to facilitate discussion of results:

1. Low alloys (0 to 3% alloy content).
2. Intermediate alloys (3 1/2 to 6% alloy content).
3. High alloys (7 to 15% alloy content).

Titanium sponge and electrolytic iron, manganese, chromium and powder-metallurgy molybdenum were used in preparing the alloys. The alloys were arc melted in a single electrode furnace in which the arc was maintained between a water-cooled tungsten electrode and the charge, which was placed in a water-cooled copper crucible. All melting was done under an atmosphere of helium and argon, the mixture of gases being adjusted for optimum melting operation.

The furnace charge consisted of a mixture of titanium sponge and master alloys. The compositions of the master alloys were 65% chromium, 35% titanium for the chromium alloys and 50% alloy element and 50% titanium for the other alloys. All of the master alloys except the molybdenum master alloy were prepared by arc melting mixtures of titanium sponge and the alloying elements. The molybdenum master alloy was prepared by powder-metallurgy methods. The purpose for using master alloys was to provide a more uniform furnace charge for melting. The master alloys increased the volume of the alloying element with respect to the volume of the titanium sponge and made it easier to prepare a homogeneous furnace charge. Certain requirements were necessary for the furnace charge to be used in arc melting. The titanium sponge and master alloys were screened to a plus 1/8-in. and a minus 1/2-in. particle size. The residual magnesium chloride in the titanium sponge was removed prior to melting to minimize spatter during the melting operation. This was accomplished by leaching the sponge material in methanol. The sponge was then furnace and vacuum dried before melting.

The alloy ingots were melted twice to obtain plate of uniform composition. First, ingots were prepared by melting the titanium sponge and master alloys. These ingots were then forged, rolled to 1/8-in. sheet, grit blasted, sheared into 1/4- by 1/4-in. chips, cleaned and remelted in the arc furnace.

Twenty-five-pound ingots of the experimental alloys were prepared, forged and rolled to 1/2-, 1/4- and 1/8-in. plates for welding tests. Strips 1/8 in. wide were sheared from the 1/8-in.-thick plates for the filler metals used in making the welds. In the case of the chromium- and molybdenum-alloy series, small 1-lb ingots were the first available and were used in welding tests in 1/8-in. thicknesses. Generally, the forging temperature was 1700° F, and the plates were rolled at 1400° F. However, for some of the high alloys in the large ingots, forging was done at 1900° F, and the plates were rolled at approximately 1500° F.

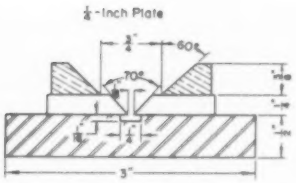
The surface scale which formed on the plates during forging and rolling was removed by grit blasting. After grit blasting, the plates were heat treated. The heat treatments given to the base metals prior to welding and testing were as follows: (1) the commercially pure titanium alloy was heated to 1500° F for 1 hr and air cooled; (2) the four series of experimental alloys were heated to 1350° F for 1 hr, furnace cooled at a rate of about 3 deg per minute to 1150° F and quenched in water.

Difficulties were encountered in preparing large ingots of the 15% chromium and 15% molybdenum alloys. The plates prepared from large ingots of these alloys had very low bend ductility and were considered

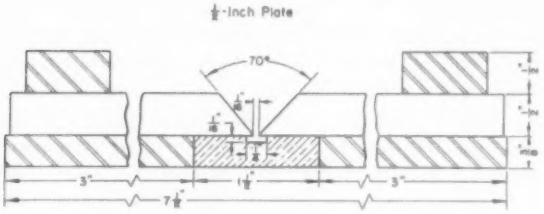
Table 1—Compositions of Titanium Alloys

Nominal composition, %	*Actual composition, wt %* — *1/4- and 1/2-in. plates**	*1/8-in. plates**
Iron alloys:		
1/2	0.52	0.52
2	1.80	1.80
3 1/2	3.16	3.16
5	4.7	4.7
7	7.3	7.3
Manganese alloys:		
1	0.88	0.88
3	2.85	2.85
6	5.88	5.88
10	9.3	9.3
15	14.6	14.6
Chromium alloys:		
1	0.90	0.86†
3	2.82	3.00†
6	5.76	5.40†
10	9.92	9.0 †
15	....	13.0 †
Molybdenum alloys:		
1	1.10	0.70†
3	3.08	2.55†
6	5.8	5.1 †
10	8.9	9.7 †
15	14.6 †	14.5 †

* Alloy filler metals were prepared from the same materials as the base plates.
† Plates prepared from small ingots.



Layer	Amperage	Voltage	Electrode diameter, in	Rate of travel, in./min
1	100	24/26	3/32	4
2	100	24/26	3/32	4



Layer	Amperage	Voltage	Electrode diameter, in	Rate of travel, in./min
1	100	24/26	1/8	5
2	100	24/26	1/8	5
3	100	24/26	3/32	4
4	100	24/26	1/16	4

Fig. 1 Joint designs and welding conditions

unsatisfactory for the study. However, a small ingot of 15% molybdenum was melted and rolled to ¼-in. plate for welding tests.

WELDING PROCEDURES

Plate-edge preparation for welding consisted of beveling the ¼- and ⅛-in. plates by grinding to form a single vee with an included angle of 70 deg for the ¼-in. plates and 90 deg for the ⅛-in. plates. The ½-in. plates were beveled by sawing to form a single vee with an angle of 70 deg. The root face in all thicknesses of material varied from 0 to 1/32 in. Prior to the welding operation, the specimens were pickled in a 4% hydrofluoric 10% nitric acid solution, washed free of acid and wire brushed. The surfaces of the specimens thus prepared were bright and appeared to be free of film.

Details of the assemblies for welding the ½- and ¼-in. specimens are shown in Fig. 1. The ⅛- and 1/16-in. specimens were placed on carbon steel backing plates and held in place with copper bars and C-clamps. The ½-in. weld specimens were placed in carbon steel jigs having grooved copper bars which served as backings for the welds. Root spacing was approximately 1/16 in. in all thicknesses of plate material.

All welds were made manually with a water-cooled tungsten-arc torch, using straight polarity, direct current in an airtight chamber filled with helium. The welding chamber was used to minimize contamination during the welding operation. Satisfactory welds probably could be made in many of the alloys without the use of the welding chamber. However, in a fundamental study of this nature, it was considered desirable to minimize the variables which might affect welded-joint properties. The welding conditions and welding chamber are shown in Figs. 1 and 2, respectively. Single-layer welds were made in the ⅛-in. plates, two-layer welds were made in the ¼-in. plates, and four-layer welds were made in the ½-in. plates. In the ½-in. plates the final weld layer consisted of two passes.

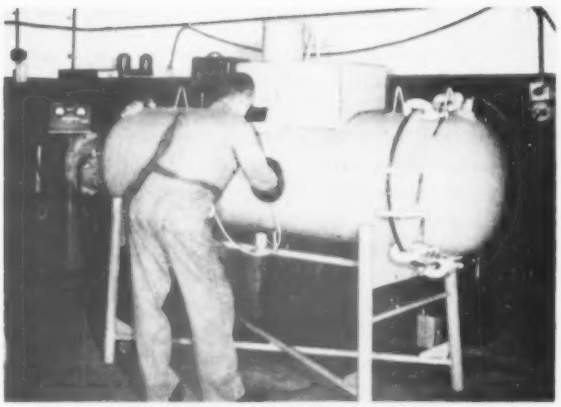
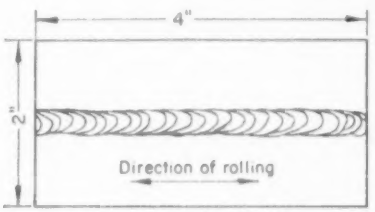
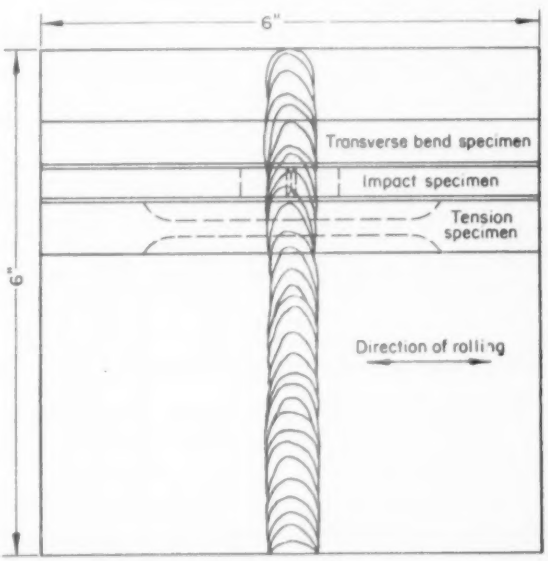


Fig. 2 Controlled-atmosphere welding chamber



a Longitudinal Bend Specimen



b Orientation of Test Specimens in Welded Plates

Fig. 3 Specimens for weld-joint tests, ¼-in. plate

The types of test specimens prepared are shown in Fig. 3. Longitudinal bend-test specimens were prepared in the 1/4- and 1/8-in. alloys with the weld parallel to the direction of rolling of the plate. In the 1/4-in. plates, specimens were prepared also with the weld transverse to the direction of rolling of the plates. The latter weld specimens were sectioned, as shown in Fig. 3, and were tested for transverse bend ductility, joint tensile strength and notch-toughness properties. The plates for the 1/2-in. weld specimens were 2 1/2 in. wide and 6 in. long. These plates were prepared with a weld 6 in. long transverse to the rolling direction of the plates. The 1/2-in. weld specimens were sectioned for making transverse bend and notch-toughness tests.

The filler metals used for making most of the welds matched the composition of the base materials. However, longitudinal weld bend specimens were prepared in the four series of 1/4-in.-thick alloys using both alloy and commercially pure filler metals.

TEST PROCEDURES

The tests performed on the base metals and welded joints consisted of tensile, bend and notch-toughness tests, hardness studies and metallographic examinations. The welds were examined visually for surface defects and freedom from contamination. After visual examination, the welds were radiographed to determine their soundness before removal of the test specimens.

The base-metal and welded-joint tension test specimens were of the flat-bar type with a reduced section 2 in. long. The cross-sectional dimensions of the reduced section were 1/8 by 1/4 in. The yield strength for each alloy was determined at 0.2% offset, and the elongation was measured over a 1-in. gage length. In the joint tension test specimen, the weld was in the center of the reduced section.

Bend tests were made on the base metals with the strain in the direction of rolling of the plate. Guided bends were made whereby bending was performed using dies of different radii, starting with those with large radii, and reducing the die radius until failure of the specimen occurred.

Longitudinal bend tests were made to determine the ability of the metal in different zones in the welded joint to deform plastically together. Straining was produced in the direction parallel to the weld by making guided bends. Failure was considered to have occurred in the bend tests when an open defect measuring over 1/16 in. in any direction was observed. The bend ductility of the 1/8-in. specimens was calculated from the minimum die radius over which the specimens could be bent before failure occurred. A grid was made on the 1/4-in. specimens, and the bend ductility was measured over a 1/2-in. gage length.

Transverse bend tests were made on welds in the 1/4- and 1/2-in. thick plates, wherein the direction of strain was normal to the weld. The bend specimens were 1/2 in. wide and 6 in. long in the 1/4-in. plates and 3/4 in. wide and 5 in. long in the 1/2-in. plates. Weld reinforcements were removed prior to testing. The 1/4-in. transverse bend specimens were tested as free bends, and the elongation was measured over a 1/2-in. gage length by the use of a grid. The 1/2-in. specimens were tested as guided bends, and again elongation was measured over a 1/2-in. gage length. Some transverse bend tests were made also on specimens cut from 1/4-in. longitudinal bend specimens. These specimens were tested by making guided bends.

Notch-toughness tests were made on welds in the 1/4- and 1/2-in. plates. For the tests in the 1/4-in. plates, a microimpact specimen was used. The design of this specimen, which is being used in other titanium investigations at Battelle, was based on the cylindrical Izod Type Y specimen with a circumferential notch. All

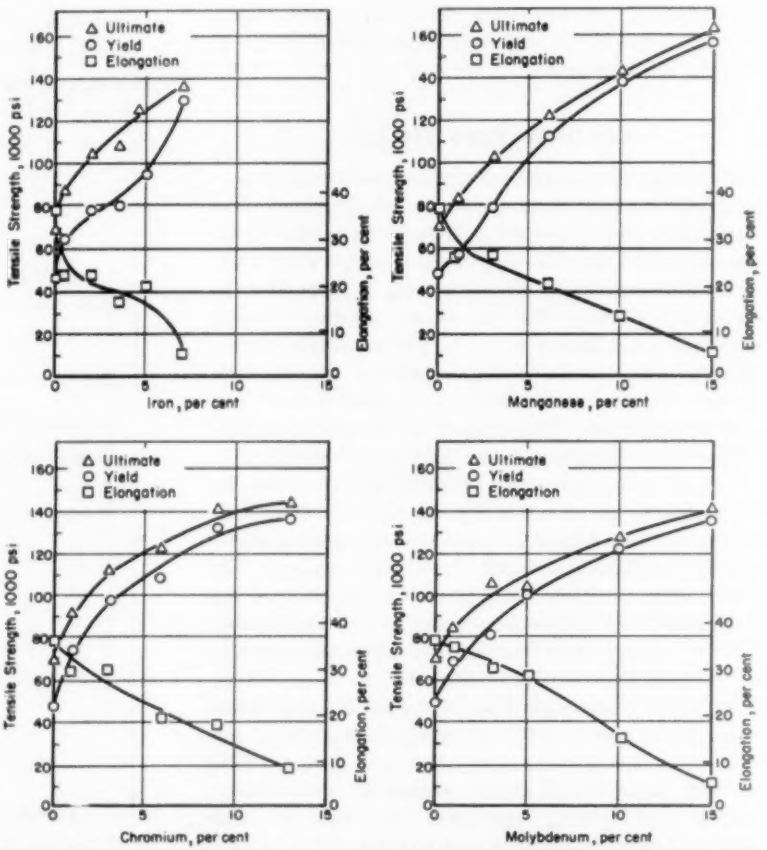


Fig. 4 Effect of composition on mechanical properties of 1/8-in. alloy plate (alpha-beta heat-treated condition)

dimensions were reduced to $^1/_2$ scale, including the notch radius. The round-bar specimen was $1^1/_8$ in. long with a diameter of 0.225 in. The 45-deg notch was 0.0375 in. in depth with a 0.005 in. radius at the root and was located in the center of the specimen. The specimen was held vertically and broken as a cantilever beam in a constant-velocity (11.34 fps) Tinius Olsen impact testing machine. The notch-toughness tests conducted on welds in $^1/_2$-in. plates were made using a standard vee-notch Charpy bar.

Correlation curves[1] have shown that the notch-toughness values obtained from microimpact specimens in inch-pounds are approximately equivalent numerically to the values obtained in foot-pounds from vee-notch Charpy specimens. To obtain equivalent vee-notch Charpy values, the microimpact values in inch-pounds should be multiplied by the factor 10/12.

Hardness surveys were made on cross sections of the welded joints. Vickers hardness measurements were made using a diamond pyramid indenter and a 10-kg load. Hardness readings were taken at 0.03-in. intervals, starting in the base metal and proceeding through the heat-affected zone and into the weld metal. A single traverse was made across the $^1/_8$-in. welds. Two traverses were made across the $^1/_4$-in. welds to determine if there was a hardness variation between the first and second layers of the welded joint. Hardness traverses were made normal to the surface in the $^1/_2$-in. welds to determine if variations in hardness existed in the different weld layers.

Microstructural examinations of the welded joints were made to determine if a correlation existed between variations in structure and welded-joint properties.

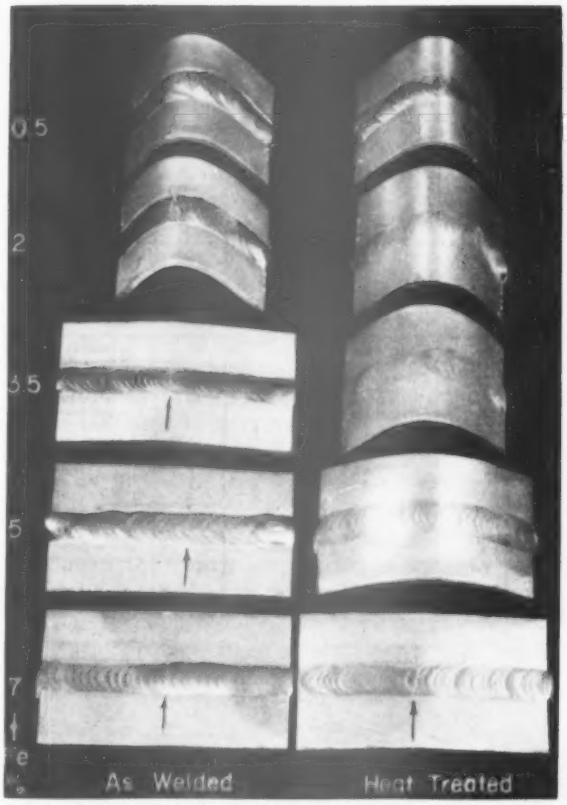


Fig. 6 Longitudinal bends in $^1/_8$-in. titanium-iron welds (bend results typical of those obtained in the four series of alloys, data shown in Fig. 7)

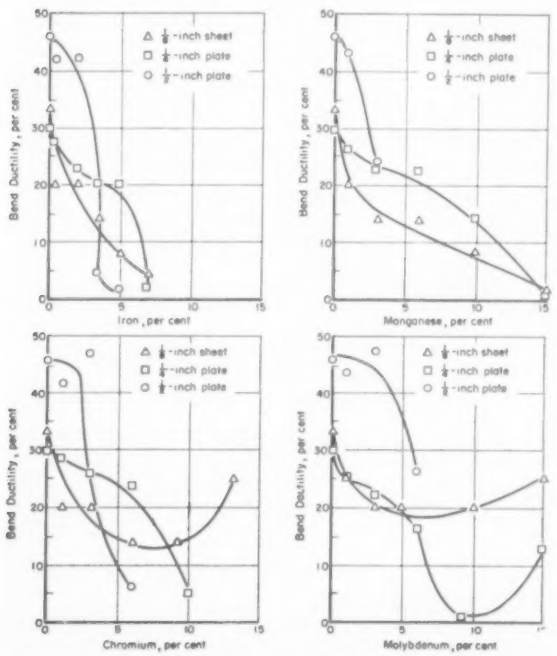


Fig. 5 Bend ductility of unwelded alloy plates (strained parallel to rolling direction of plates)

The examinations were made also to determine the effects of heat treatment on the microstructures of the weld.

The welds studied in this investigation were tested in both the as-welded and heat-treated conditions. The heat treatments were carried out in stainless steel retorts which were purged of air and had a steady flow of argon maintained through them during the operation. This was done to protect the base metal and weld surfaces from contamination during the heat treatments. If the heat treatments required quenching from temperature, the retort was opened and the materials were quenched in water. However, if a slow cool from temperature was required, the specimens were left in the retort during cooling.

POSTWELD HEAT TREATMENT

The postweld heat treatments used in this study and their designations are listed below:

1. An alpha-beta heat treatment, which consisted of heating the specimens to 1350° F, furnace cooling at a rate of approximately 3 deg per minute to 1150° F, followed by water quenching.
2. A beta-quench heat treatment, which consisted

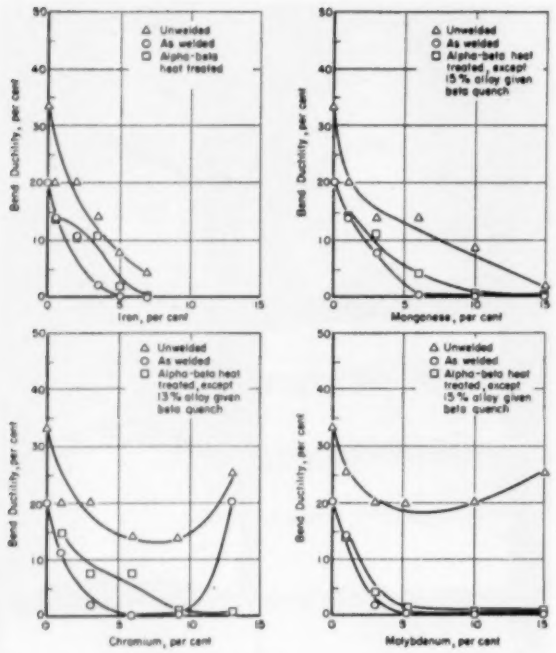

Unwelded
As welded
Alpha-beta heat treated
Alpha-beta heat treated, except 15% alloy given beta quench
Alpha-beta heat treated, except 13% alloy given beta quench
Bend Ductility, per cent
Iron, per cent
Manganese, per cent
Chromium, per cent
Molybdenum, per cent

Fig. 7 Longitudinal bend ductility of welded and unwelded 1/4-in. alloy plates

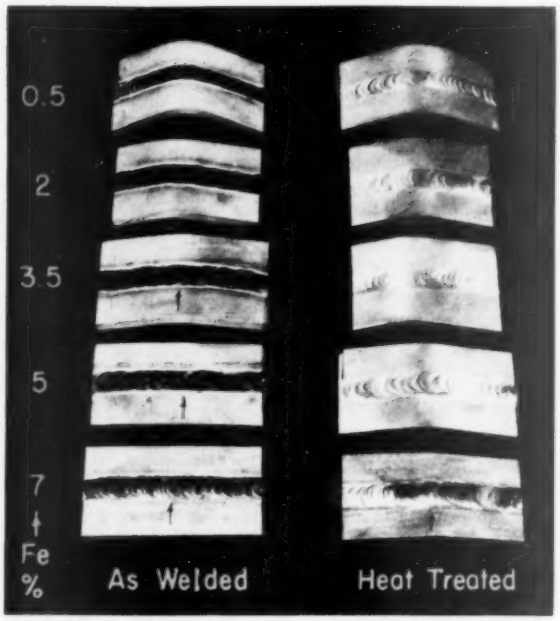


Fig. 8 Longitudinal bends in 1/4-in. titanium-iron welds (bend results typical of those observed in the four series of alloys, data shown in Fig. 9)

of heating the specimens into the beta field and water quenching.

3. A beta-anneal heat treatment, which consisted of heating the specimens into the beta field and furnace cooling.

The alpha-beta heat treatment was designed to overcome transformation hardening which occurred in welding and to cause maximum alpha transformation to take place, thus stabilizing the alloys. The specific parts of the alpha-beta heat treatment were selected for the following reasons:

1. The initial holding temperature of 1350° F is high in the alpha-beta field. At this temperature, diffusion is rapid and the rate of transformation of beta to massive alpha is high enough so that long holding times are not required to approach equilibrium. This treatment softens the hard transformed-beta structure which occurs in some of the weld metals and heat-affected zones in the as-welded joints.

2. The slow cool from 1350 to 1150° F was selected to further stabilize the beta phase of the alloy, causing a maximum amount of alpha rejection from the beta phase so that the alloy would be less susceptible to aging which results from beta instability.

3. The water quench from 1150° F was used to cool the high alloys rapidly enough to prevent possible precipitation hardening. In all probability, an air cool would have been sufficiently fast for this purpose.

The beta-quench postweld heat treatment was used on most of the welds in high metastable-beta alloys, because it is believed to give optimum mechanical properties to these alloys. Base metals of these alloys generally show maximum strength and ductility combinations in the all-beta condition. The heat treatment was used to place the alloy in the all-beta condition and to cool it rapidly enough to prevent the precipitation of alpha.

The beta-anneal heat treatment was used on some of

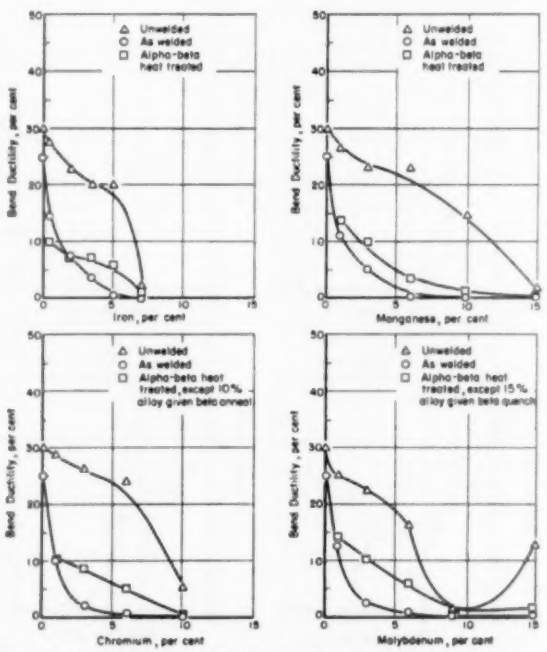

Unwelded
As welded
Alpha-beta heat treated
Alpha-beta heat treated, except 10% alloy given beta anneal
Alpha-beta heat treated, except 15% alloy given beta quench
Bend Ductility, per cent
Iron, per cent
Manganese, per cent
Chromium, per cent
Molybdenum, per cent

Fig. 9 Longitudinal bend ductility of welded and unwelded 1/4-in. alloy plates

the longitudinal weld bend specimens made in the low and intermediate alloys. For these alloys, the slow cool through the transformation range permits the beta-to-alpha transformation to proceed by a nucleation and growth process. A further value of this heat treatment lies in the fact that, except for the columnar grains in the weld metal, it places all zones in the welded joints in practically the same structural condition. The disadvantages are that this heat treatment requires taking the alloys to higher temperatures where risk of contamination is greater than for the lower temperature heat treatment. Distortion and sagging may occur also because of the lower strength of the alloys at temperatures in the beta field. This heat treatment probably would cause embrittlement in the high alpha-beta and metastable-beta alloys because of the slow cool at temperatures where precipitation hardening is likely to occur.

Other heat treatments could have been used to improve weld ductility, and in some cases greater improvement might have been obtained had other heat treatments been used. However, it is believed that these heat treatments provided an indication of the value of postweld heat treatments for improving welded-joint ductility.

DISCUSSION AND RESULTS

The tests used in conducting this investigation were not designed to establish all of the properties of the alloys and welded joints studied, but were planned to obtain information which would show the trends developed in each alloy series. For this reason also, one heat treatment was used on all base materials instead of selecting a heat treatment for each alloy studied. The results presented are from single specimens unless otherwise noted. Single specimens were considered satisfactory for an investigation of this type, because in studying a series of alloys relationships are established that serve as a check on the individual test results. In presenting the results obtained, nominal alloy compositions are used in referring to the alloys. The data obtained from tests on the commercially pure titanium metal are included in all curves at 0% alloy.

Preliminary examination of the welds showed that the surfaces were shiny, indicating a minimum of contamination during the welding operation. Welds which were discolored were rejected. Radiographs showed the welds to be sound except for a slight amount of fine porosity in some of the welds. This fine porosity was found also in the microscopic examina-

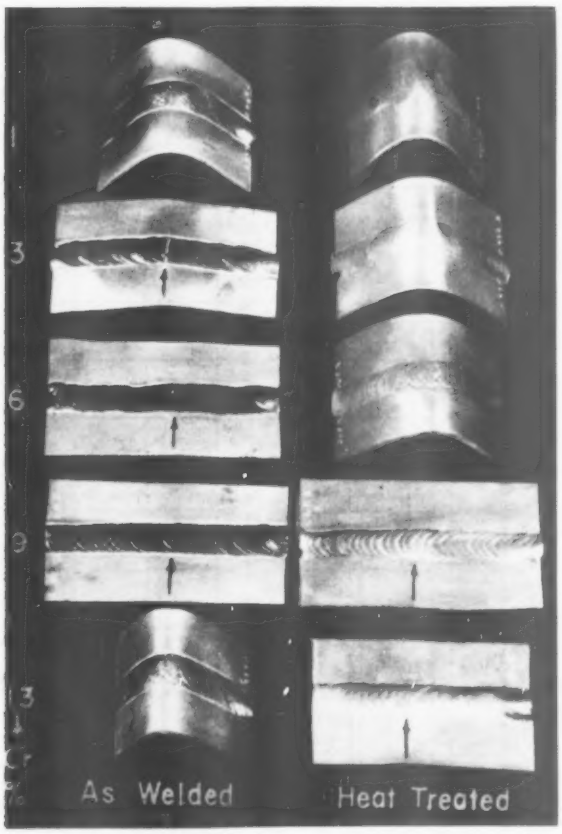


Fig. 10 Longitudinal bends in 1/8-in. titanium-chromium welds

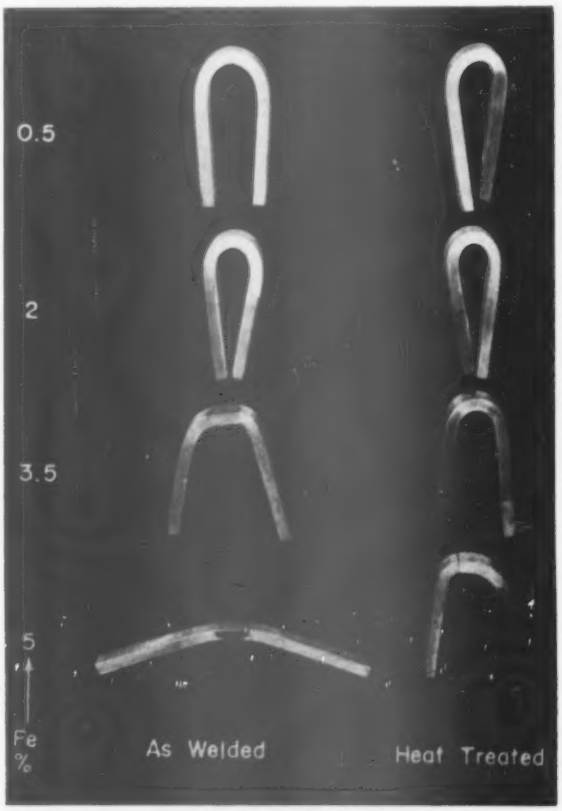


Fig. 11 Transverse bends in 1/8-in. titanium-iron welds (bend results typical of those observed in the four series of alloys, data shown in Fig. 12)

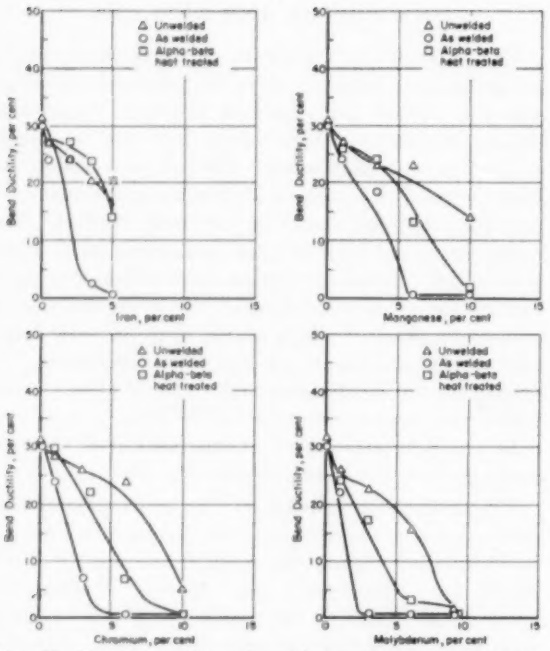


Fig. 12 Bend ductility of welded and unwelded 1/4-in. plates (strained transverse to weld and parallel to rolling direction of plate)

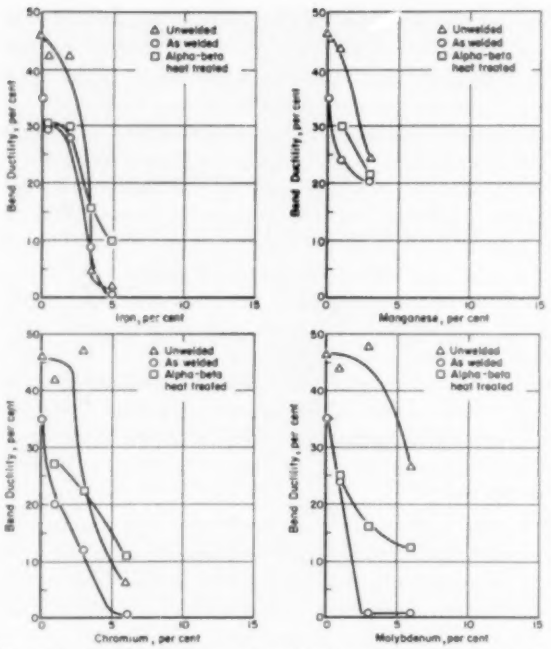


Fig. 14 Bend ductility of welded and unwelded 1/2-in. plates (strained transverse to weld and parallel to rolling direction of plate)

tions of the welds. Transverse cracks were found in the 15% manganese weld specimen before testing. No cracks were observed in the welds of the other alloys.

Tensile Tests

The tensile properties of the 1/8-in. alloy-plate materials tested in this program are plotted in Fig. 4. It was found that the tensile and yield strengths of the stabilized alloys increased, and the elongation decreased with increasing alloy content. Generally, the ultimate strength increased from about 70,000 psi for the commercially pure metal, to about 140,000 to 160,000 psi for the high alloys. The tensile properties of the 1/4-in. plates were very similar to those shown for the 1/8-in.-thick materials.

Joint tension tests were made on welds in the 1/4-in. plate. The specimens were oriented with the welded

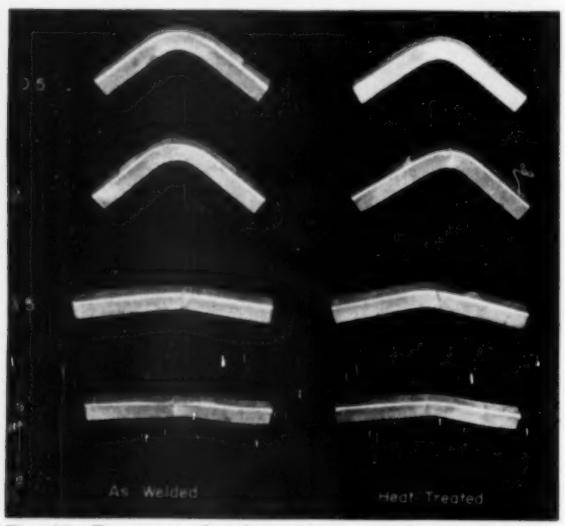


Fig. 13 Transverse bends in 1/2-in. titanium-iron welds (bend results typical of those observed in the four series of alloys, data shown in Fig. 14)

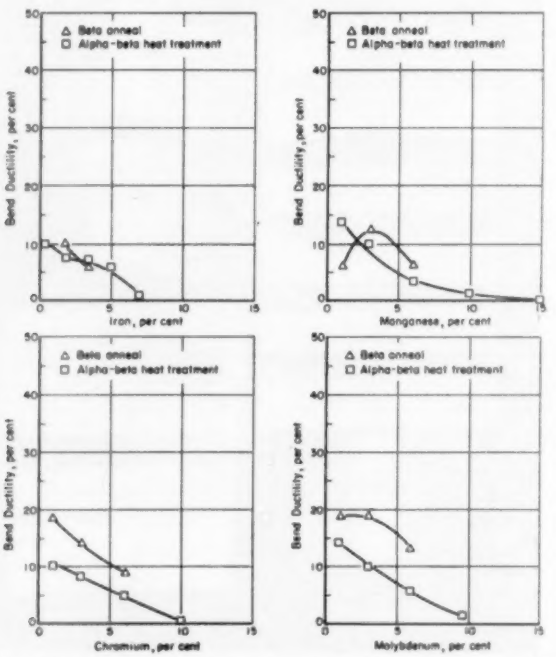


Fig. 15 Longitudinal bend ductility of heat-treated welds in 1/4-in. plates

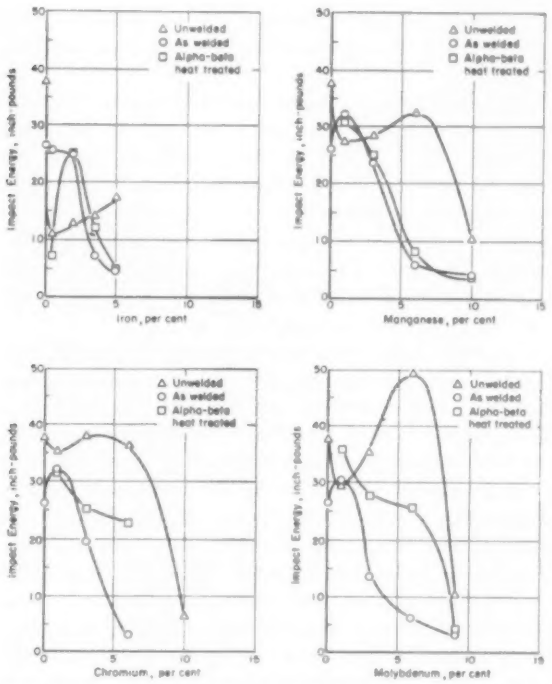


Fig. 16 Room-temperature notch-bar tests for welded and unwelded 1/4-in. plates (round-bar specimen, 0.225-in. diam, broken as a cantilever beam)

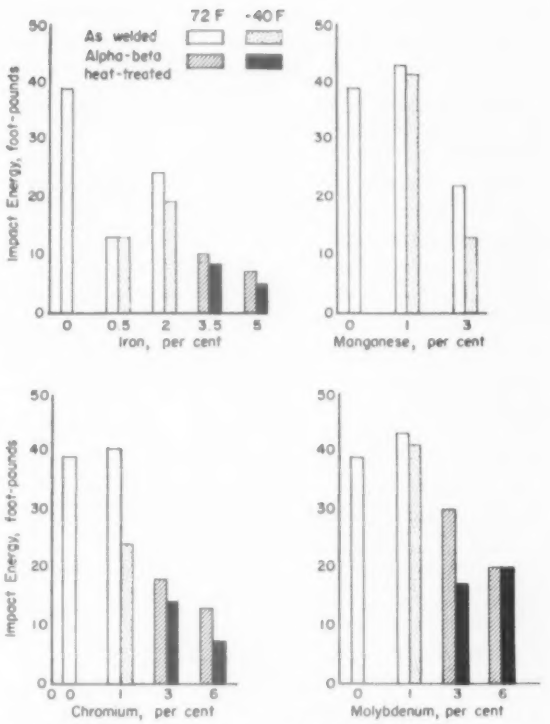


Fig. 18 Notch-bar tests on weld metals in 1/2-in. plate (vee-notch Charpy specimens)

joint in the center of the reduced section of the specimen. Practically all of the joint tension test specimens failed in the base metal at stress levels very similar to those which produced failures in the base-metal tension test specimens. The results of the joint tension tests on postweld heat-treated specimens are shown in Table 2. These tests showed that the weld metals and heat-affected zones had greater strengths than the base metals.

Bend Tests

The bend ductility of the 1/8-, 1/4- and 1/2-in. base metals are shown in Fig. 5. The bend ductility of the iron and manganese alloys decreased with increasing alloy content throughout the alloy series. It decreased from a value of 30 to 45% for the commerically pure metal to practically zero for the 7% iron and 15% manganese alloys. In the case of the chromium- and

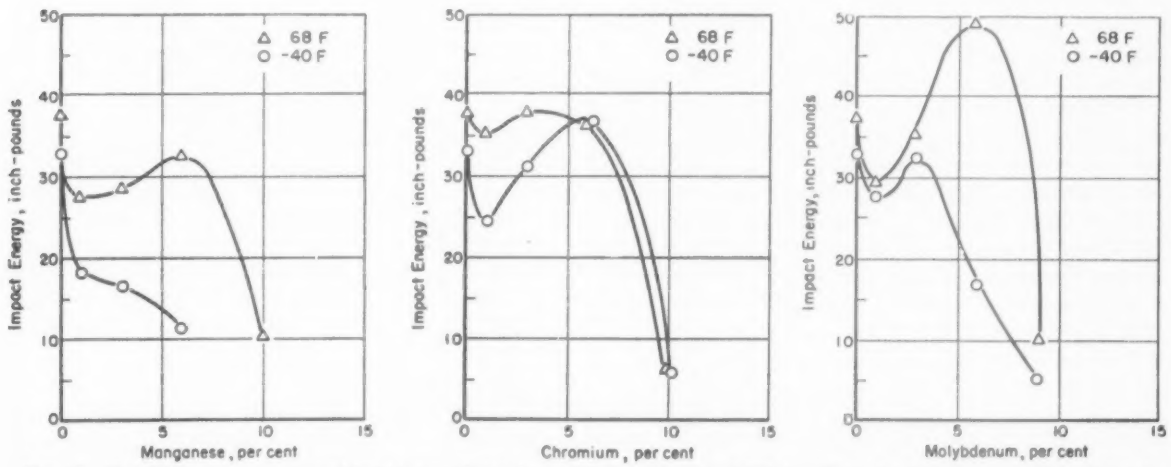


Fig. 17 Notch-bar tests on unwelded 1/4-in. plates (round-bar specimen, 0.225-in. diam, broken as a cantilever beam)

Table 2—Joint Tensile Strength of 1/4-In. Welded Plates

Nominal composition, %	Ultimate tensile strength, psi*: Base-metal specimen	Ultimate tensile strength, psi*: Joint tension test specimen†
Commercially pure titanium	77,100‡	67,000§
Iron alloys:		
1/2	87,300	88,500
2	101,500	104,000
3 1/2	106,900	103,200‖
5	117,000	115,300
Manganese alloys:		
1	82,000	83,100
6	119,900	118,500
10	141,000	125,400¶
Chromium alloys:		
1	88,300	91,200
3	105,100	106,200
6	123,700	123,200
10	145,000	139,000‖
Molybdenum alloys:		
1	92,800	96,400
3	96,600	97,300
6	111,500	106,900
9	132,200	132,500

* Alpha-beta heat-treated condition.
† Failed in base metal.
‡ Heated to 1500° F for 1 hr and air cooled.
§ Tested in the as-welded condition.
‖ Tool mark on specimen.
¶ Failed in weld metal.

molybdenum-alloy series, the bend ductility decreased with increasing alloy content through the alpha-beta alloys and then increased for the metastable-beta alloys. In the 1/8-in. alloys of molybdenum and chromium, the bend ductility of the alloys was good for all compositions studied.

Longitudinal bend tests were made on welded joints in the 1/8- and 1/4-in.-thick alloy plates. The results obtained from these tests and typical examples of the welded specimens tested are shown in Figs. 6–9. The results shown are for welded joints made with filler metals of the same composition as the base metals.

The trends observed in the single-layer welds in the 1/8-in.-thick plates and the two-layer welds in the 1/4-in.- thick plates were very similar. The bend ductility of welds in the alpha-beta alloys decreased rapidly with increasing alloy content. It appears that in order to produce ductile as-welded joints in the alpha-beta alloys, the beta-stabilizing alloy content must be held to a relatively low level. This is shown by the fact that the commercially pure titanium metal and the alloys containing up to 3% manganese, 1% chromium, 2% iron and 1% molybdenum produced welded joints with fair bend ductility in the as-welded condition; whereas, alloys of the next higher alloy content in each series had very low bend ductility as welded.

Postweld heat treatment improved the bend ductility of welded joints in alloys with up to 6% alloy content. The alpha-beta heat treatment which was used on welded joints in these alloys resulted in slight to moderate improvement in bend ductility. Only slight improvement was generally found in welds which had fair bend ductility as welded; whereas, substantial improvement was found in some of the welds which had low bend ductility as welded.

The bend ductility of the alpha-beta alloys with over 6% alloy content was very low both as welded and heat treated. The welds did not appear so brittle after heat treatment as they did in the as-welded condition, but no measurable increase in ductility was found.

With one exception, the welded joints in the metastable-beta alloys had low bend ductility in both the as-welded and heat-treated conditions. The exception was the 13% chromium alloy in which the single-layer welded joint in the 1/8-in.-thick plate had very good ductility as welded but was embrittled by the beta-quench heat treatment from 1400° F. Bend specimens in the chromium-alloy series are shown in Fig. 10 and

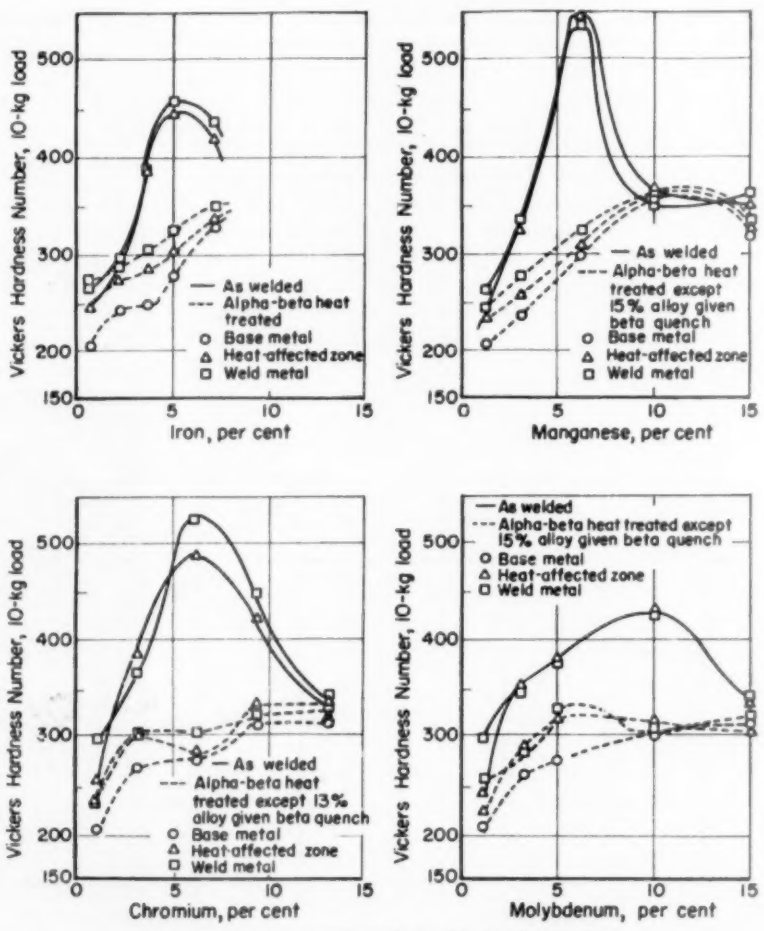


Fig. 19 Hardness values for welds in 1/8-in. plate

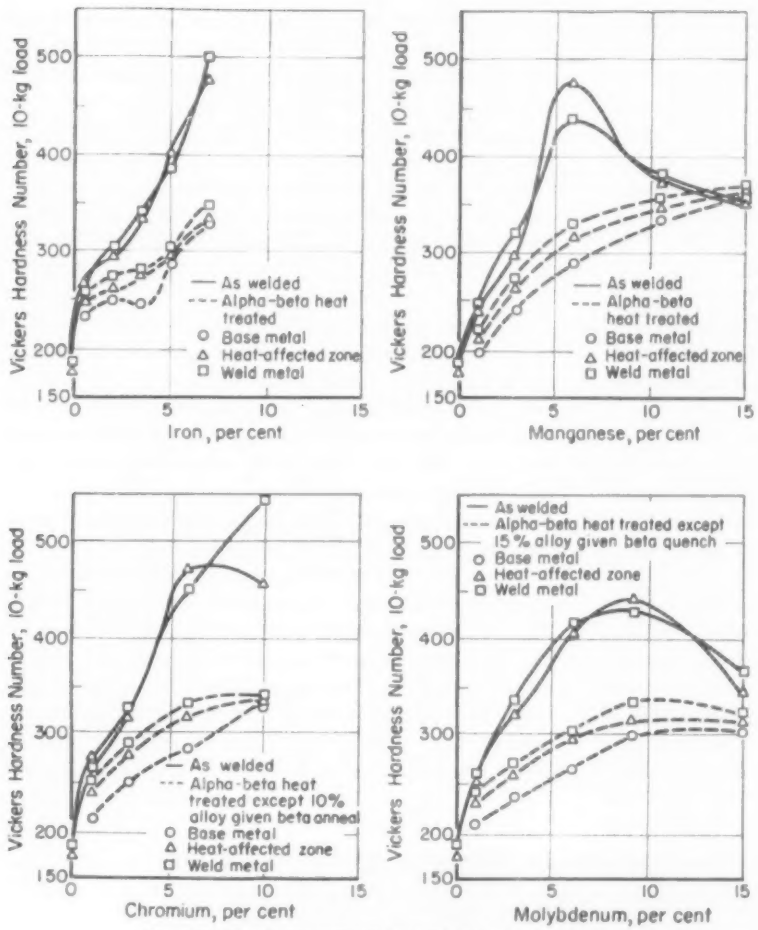


Fig. 20 Hardness values for welds in 1/4-in. plate

indicate the excellent bend ductility found in the weld in the 13% chromium alloy. In the case of welded joints in the 15% molybdenum alloy, the base metal was ductile but the weld was brittle; whereas, in the case of the 7% iron and 15% manganese alloys, the base metal also had low bend ductility.

The major differences between the bend test results on the single- and two-layer welded joints appeared to be the location of failure. The failures in the single-layer welds in the 1/8-in. specimens generally occurred in the weld metal. Failures in the two-layer welds in the 1/4-in.-thick specimens generally occurred in the heat-affected zones in the welded joints which had some ductility in either the as-welded or heat-treated conditions. These heat-affected-zone failures apparently initiated from microcracks which appeared in these zones early in bending operation. The cracks at this stage were limited to a length of from 0.03 to 0.05 in., and began to propagate only after considerable additional bending had been accomplished.

Transverse bend tests were made on welded joints in 1/4- and 1/2-in.-thick plates. The welded joints were made with filler metals of the same composition as the base metals. The results obtained from these tests and typical examples of the specimens tested are shown in Figs. 11–14.

The transverse bend tests gave results similar to those obtained in making longitudinal bends. The bend ductility of the as-welded joints decreased rapidly with increasing alloy content and became low when the alloy content reached 3 to 6%. Generally, the transverse bend ductility of welded joints with fair bend ductility was higher than the longitudinal bend ductility of welded joints in the same alloys. The mechanisms which caused the heat-affected-zone failures to occur in the 1/4-in. longitudinal bend specimens did not appear to cause heat-affected-zone failures in the transverse bend specimens.

Longitudinal weld bend specimens were prepared in the 1/4-in. alloy plates using commercially pure filler metal. The results obtained in bending these specimens were about the same as those obtained in testing longitudinal bend specimens of welds made with alloy filler metals. Failures in the specimens welded with commercially pure filler metals occurred in the heat-affected zone. Small transverse bend specimens were cut from longitudinal specimens in the iron-alloy series and bent with maximum strain occurring in the weld metal. The welded joints in all but the 7% iron alloy had fairly good transverse bend ductility. Practically all deformation occurred in the weld metal.

In the results presented above, the alpha-beta heat treatment was used on the welds in the alpha-beta alloys, and the beta-quench heat treatment was used on welds in the 13% chromium, 15% manganese and 15% molybdenum metastable-beta alloys. There were two exceptions to this practice. The alpha-beta heat treatment was used on the weld in the 15% manganese 1/4-in. longitudinal bend specimen, and the beta-anneal heat treatment was used on the weld in the 10% chromium 1/4-in. longitudinal bend specimen. The beta-anneal heat treatment was used also on some of the longitudinal weld bend specimens in the low and intermediate alloys. It was used in an attempt to eliminate the heat-affected-zone failures observed in longitudinal bend tests on two-layer welds in 1/4-in. plates. It was found, however, that the heat-affected-zone failures persisted in the test specimens given the beta anneal, but weld-metal and base-metal failures also occurred. Figure 15 shows a comparison between the bend ductility of welds given the alpha-beta heat treatment and welds given the beta anneal. The beta anneal appeared

to be more effective in improving the bend ductility of welds in the chromium- and molybdenum-alloy series than the alpha-beta heat treatment.

Notch-Toughness Tests

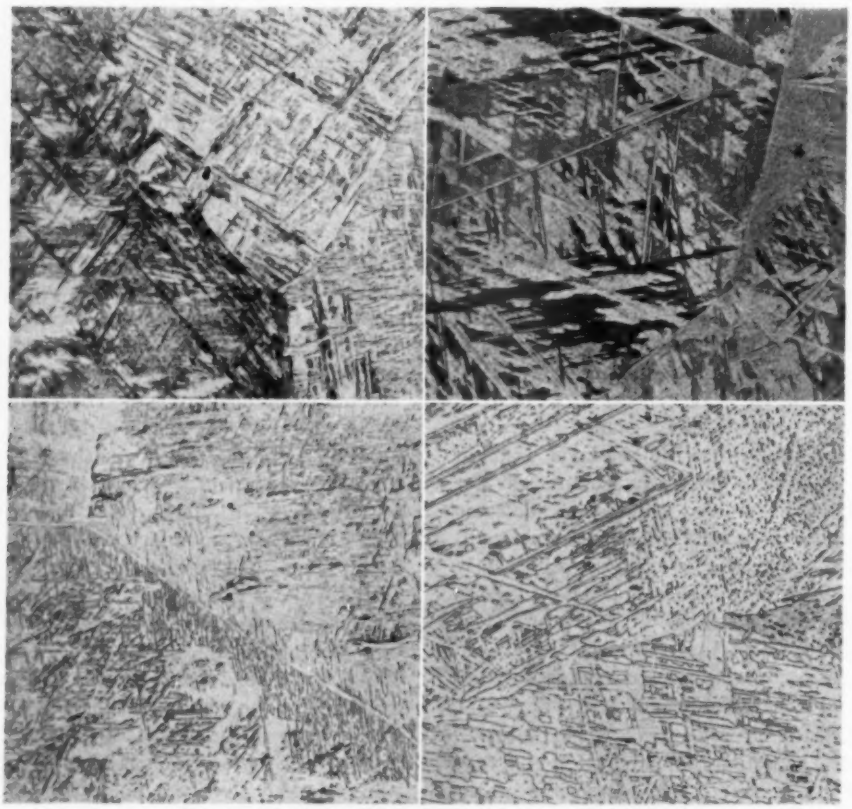
Room-temperature notch-toughness values obtained on the $^1/_4$-in. plate materials and weld metals using the microimpact specimens are shown in Fig. 16. The specimens were oriented in the testing machine so that the portion of the notch in the face of the weld was under maximum tension stress. The results shown are from single tests for all but the iron-alloy base metal. In the results for the iron-alloy base metals, the values shown are the averages of two tests.

The base-metal notch toughness of the low and intermediate manganese, chromium and molybdenum alloys was relatively good, while the notch toughness of the high alloys was relatively low. The iron-alloy base metals had fairly low notch toughness for the low and intermediate compositions, and, in some cases, the notch toughness of the weld metal was higher than that of the base metal.

Weld-metal notch toughness appears to be correlated fairly well with bend ductility. With the exception of the heat-treated welded joint in the $^1/_2$% iron alloy, welded joints which had fair bend ductility also had relatively good notch toughness. In the case of welded joints with low bend ductility, the notch toughness also was generally low. However, welded joints in the 3% chromium and molybdenum alloys had low bend ductility in the as-welded condition, but their notch toughness was relatively good. The notch toughness of some of the welds was improved by the alpha-beta heat treatment. In the welds in the chromium and molybdenum alloys, the notch toughness of the welds of the 3 and 6% chromium and molybdenum alloys was substantially increased by heat treatment. In the case of the welds in the iron and manganese alloys, heat treatment had little effect on the notch toughness of all but the $^1/_2$% iron alloy which had low notch toughness after heat treatment.

Notch-toughness tests were made also on the $^1/_4$-in. base metals at −40° F. The results of these tests are shown in Fig. 17. The notch toughness of the base metals at this temperature was generally lower than the room-temperature values but remained relatively good for most of the low and intermediate alloys.

The results of notch-toughness tests made on welded joints in $^1/_2$-in. plates are shown in Fig. 18. These tests were made on vee-notch Charpy specimens with the notch oriented normal to the weld surface to include all weld layers. They were tested in either the as-welded or heat-treated condition, depending on the condition in which they exhibited ductility in bending. These tests were made at room temperature and −40°



(a) Two per cent iron weld metal, as-welded, coarse acicular transformed-beta structure. VHN 301. × 250

(c) One per cent manganese weld metal, as-welded coarse acicular transformed-beta structure. VHN 240. × 250

(b) Same as above after alpha-beta heat treatment, alpha plates coarsened by heat treatment. VHN 275. × 250

(d) Same as above after alpha-beta heat treatment, alpha plates coarsened by heat treatment. VHN 223. × 250

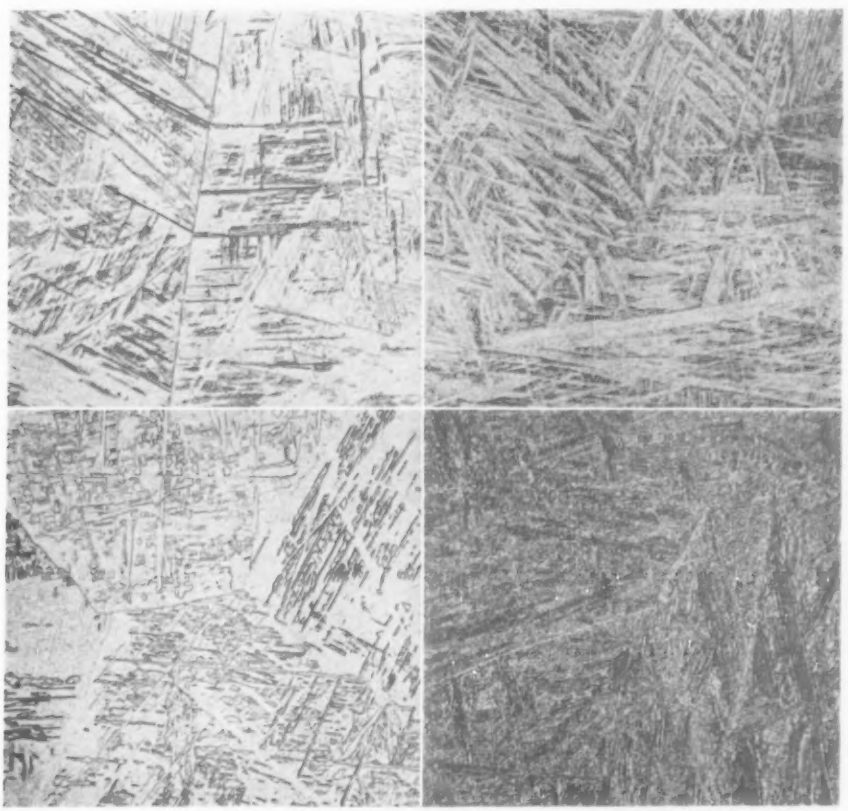
Fig. 21 Weld-metal structures in $^1/_4$-in. low alloys

F. Room-temperature results on the $^1/_2$-in. weld metals were similar to those observed in the $^1/_4$-in. weld specimens. The notch toughness of welded joints in the commercially pure metal and low alloys was higher in the $^1/_2$-in. plates than in the $^1/_4$-in. plates. The toughness values obtained on the $^1/_2$-in. welds at $-40°$ F were lower than for room-temperature tests. The values remained relatively good for most of the weld metals in the low alloys and the 6% molybdenum intermediate alloy.

Hardness Studies

The average hardness of the base metals, heat-affected zones and weld metals in the four series of alloys are shown in Figs. 19 and 20. For simplification, only one base-metal hardness curve was drawn for each series of alloys. In all but the metastable-beta alloys, the heat treatment given the welded joints was the same as the heat treatment given the base metal before welding. Consequently, the hardness of the base metals in the as-welded specimens was very similar to the hardness of the base metals in the heat-treated specimens. The hardness of the base metals increased with increasing alloy content in accordance with their increased content of strong beta phase. However, the hardness of the weld metals and heat-affected zones in the as-welded condition generally increased to a maximum and then decreased as the alloy content increased. In many of the welded joints, the hardness of the as-welded weld metals and heat-affected zones was considerably higher than the hardness of the base metals. These harder structures were formed as a result of heating the weld metals and heat-affected zones into the beta field during the welding operation and rapidly cooling. The hardening of these zones is due to the transformation of beta to alpha by a combination of the martensitic shear-type reaction and precipitation or pre-precipitation of alpha proceeding by diffusion processes. The resulting increase in hardness is called transformation hardening. Transformation hardening increased with increasing alloy content in the low and intermediate alloys and generally reached a maximum in the intermediate or high alpha-beta alloys. Except in the case of the high-iron alloy, transformation hardening decreased considerably in the metastable-beta alloys and was practically negligible for the 13% chromium and 15% manganese metastable-beta alloys.

The effect of heat treatment on transformation hardening is shown by comparing the hardness of the weld metals and heat-affected zones in the as-welded and heat-treated conditions. The alpha-beta heat treatment brought about a change from the complex structure resulting from rapid cooling from the beta field to a



(a) One per cent chromium weld metal, as-welded, coarse acicular transformed-beta structure. VHN 269. × 250

(b) Same as above after alpha-beta heat treatment, alpha plates coarsened by heat treatment. VHN 256. × 250

(c) One per cent molybdenum weld metal, as-welded, coarse acicular transformed-beta structure. VHN 260. × 250

(d) Same as above after alpha-beta heat treatment, alpha plates coarsened by heat treatment. VHN 244. × 250

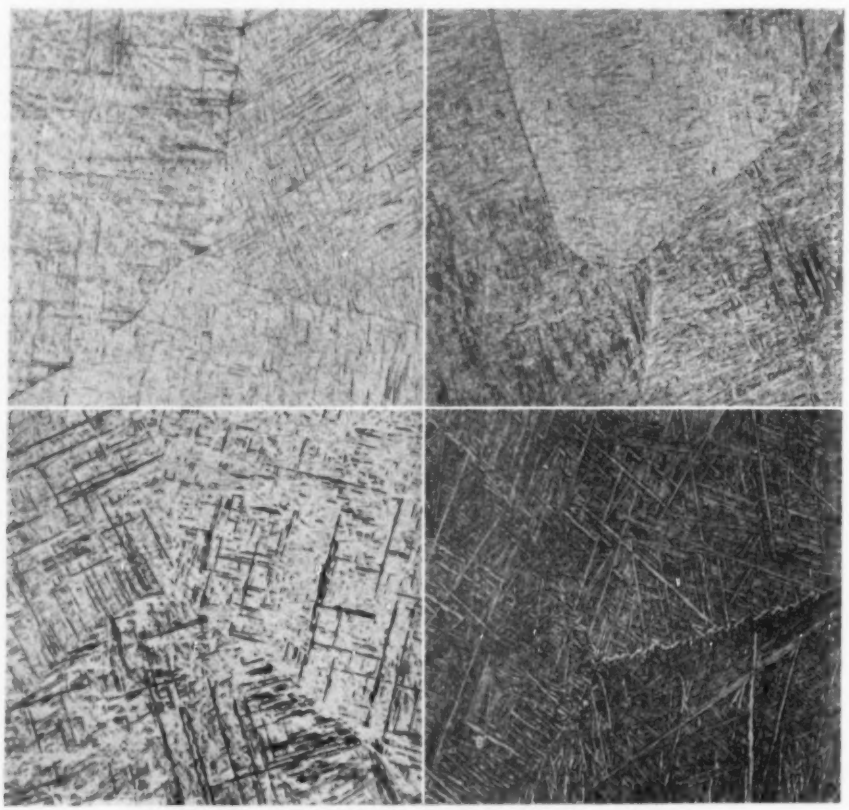
Fig. 22 Weld-metal structures in $^1/_4$-in. low alloys

simple two-phase alpha-beta structure approximating equilibrium in the range 1350 to 1150° F.

The hardness results obtained on the welds in the low and intermediate alloys appears to be correlated with the results obtained in bend tests. Transformation hardening was less pronounced in the ductile as-welded low alloys than in the intermediate alloys. This may account for the low alloys being ductile as welded, while the intermediate alloys had very low ductility as welded. The intermediate alloys had some ductility after heat treatment. This increase in ductility may be correlated with the lowering of hardness and the elimination of the hardened transformed-beta structures by heat treatment. The hardness of the weld metals and heat-affected zones in the high alpha-beta alloys was decreased by heat treatment, but the welds remained brittle. From these observations, it would appear that other mechanisms such as freezing segregation, cast structure or contamination of the weld metal may have also contributed to weld embrittlement, and that such factors apparently become more influential as the beta-stabilizing alloy content increases. This is shown also in the 13% chromium alloy, which was ductile as welded but was embrittled by heat treatment even though no hardening was observed.

The same trends were observed in the hardness results on both the single-layer $^1/_8$-in. welded joints and the two-layer $^1/_4$-in. welded joints as evidenced in Figs. 18 and 19. However, in many of the single-layer welds in the low and intermediate alloys, the as-welded hardness of the weld metals and heat-affected zones was higher than the hardness of these zones in the two-layer welds. This difference in hardness was most marked in welded joints which showed greatest transformation hardening. In the case of the higher alpha-beta and metastable-beta alloys, the hardness of these zones tended to be higher in the two-layer welds than in the single-layer welds. The hardness differences observed between the as-welded single-layer welds and two-layer welds may be caused by the differences in cooling rates in the two plate thicknesses and the effects of the two welding passes made in the two-layer $^1/_4$-in. welded joints.

Generally, only a slight difference in hardness was observed between the first layer and the second layer of weld metal deposited in the $^1/_4$-in. as-welded joints. However, in some of the welds in the intermediate and high alpha-beta alloys, a marked difference was found between the hardness of the two layers. In these cases, the hardness spread in the weld metal and heat-affected zones was very high also. In the welds in the $^1/_2$-in. iron-alloy plates, the hardness of the first layer deposited was generally less than the hardness of the last layer deposited. However, the average hardness of the weld metal in the $^1/_2$-in. plates was approximately the same as the average hardness found in the $^1/_4$-in. welds.



(a) Three per cent chromium weld metal, as-welded, fine acicular transformed-beta structure. VHN 328. × 250

(b) Same as above after alpha-beta heat treatment, alpha plates coarsened by heat treatment. VHN 291. × 250

(c) Three per cent molybdenum weld metal, as-welded, fine acicular transformed-beta structure. VHN 335. × 250

(d) Same as above after alpha-beta heat treatment, alpha plates coarsened by heat treatment. VHN 275. × 500

Fig. 23 Weld-metal structures in $^1/_4$-in. low alloys

Microstructural Examination

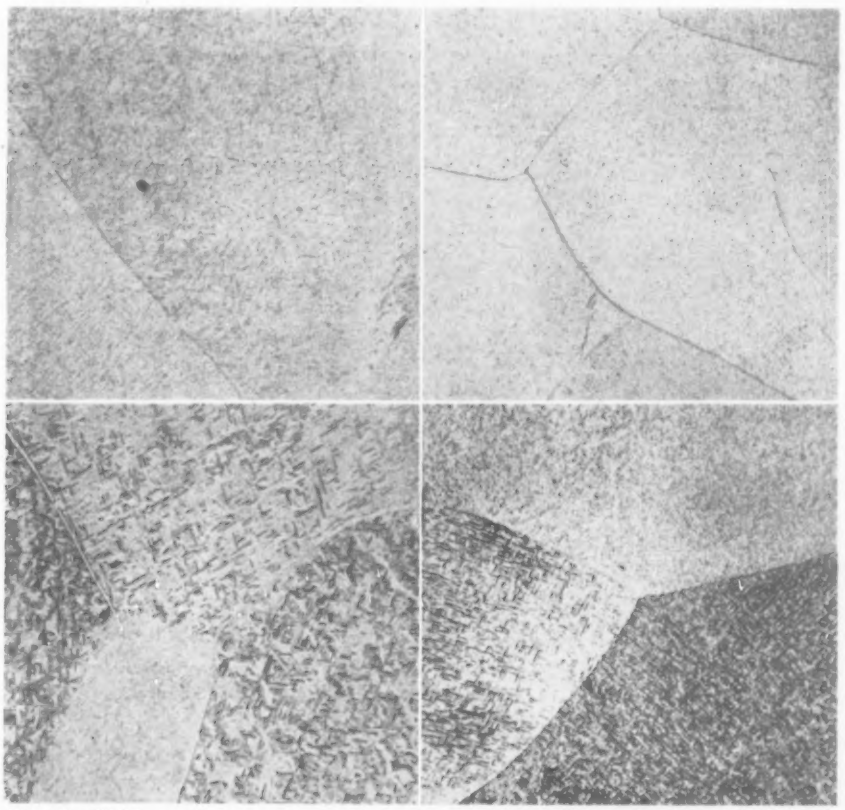
The microstructures of the weld metals and heat-affected zones changed considerably as the alloy content increased. Generally, in the low alloys, acicular-type structures were observed. These structures may be made up of martensite plates formed by a shear-type reaction or Widmanstätten plates formed by a nucleation and growth process, depending on the rate of cooling from welding. It is difficult to state positively which type of transformation resulted in these structures. Probably both processes were operative in most cases. The microstructures of weld metals are shown in Figs. 21 and 22 for some of the low alloys in the as-welded and heat-treated conditions. Welds from which these photomicrographs were made were all ductile in the as-welded condition and showed coarse acicular-type structures. The alpha-beta heat treatment did not alter these structures basically, but the alpha plates were coarsened by the treatment.

The structures of the heat-affected zones were very similar to those found in the weld metals. The major difference was that the primary beta grains were equiaxed in the heat-affected zone and columnar in the weld metal. In both cases, the grains were very large. No structural difference was observed between these zones that would account for the heat-affected-zone failures observed in longitudinal bend tests on these welds.

The structures of welds having low bend ductility in the as-welded condition and improved ductility in the heat-treated condition are shown in Figs. 23 and 24. The acicular-type structures of the weld metals of the 3% low-chromium and 3% low-molybdenum alloys were finer than those observed in the ductile as-welded low alloys. The alpha-beta heat treatment coarsened the alpha plates and improved the ductility of these alloys considerably. The structures found in the 5% intermediate iron and the 6% intermediate chromium alloys consisted of very fine precipitates of alpha in a beta matrix. This type of structure was found in most of the intermediate alloys. The alpha-beta heat treatment resulted in the precipitation of more Widmanstätten alpha and slightly improved the bend ductility of the welds in these alloys.

The type of structure found in the high-alpha-beta alloys is shown in Fig. 25. The structures shown are for the 10% manganese and 9% molybdenum alloys. The structures of the as-welded alloys are primarily retained beta with a network precipitate appearing in them. This network precipitate has been found in other investigations,[2] and is believed to be caused by the presence of oxygen and nitrogen in the alloys. The alpha-beta heat treatment produced a fine alpha precipitate in these alloys, but the bend ductility was not improved measurably.

The microstructures of the as-welded and heat-



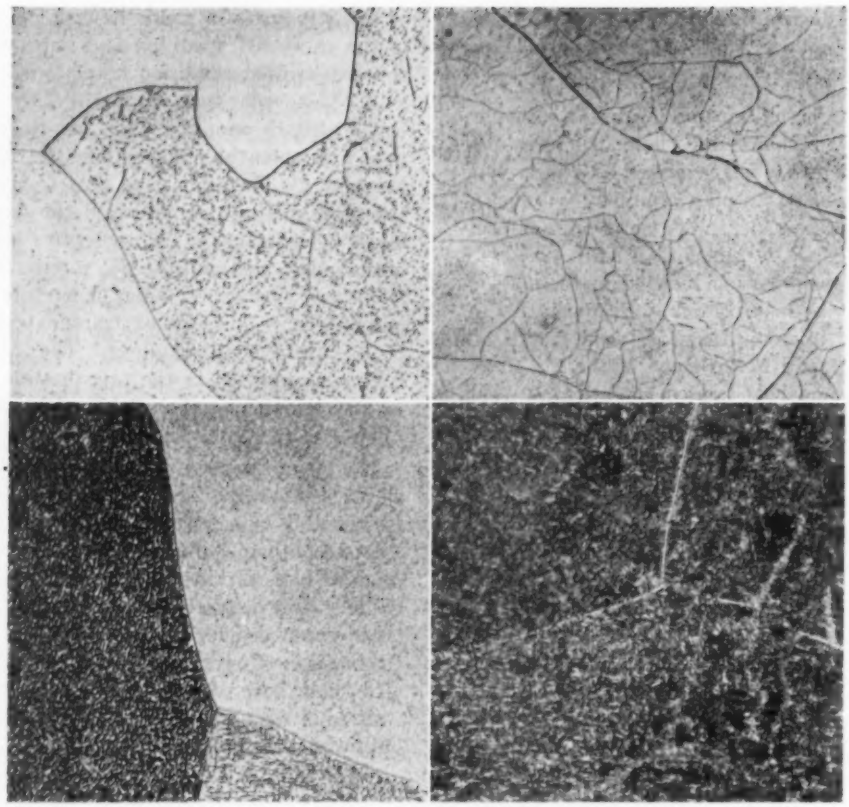
(a) Five per cent iron weld metal, as-welded, very fine Widmanstätten alpha precipitate in beta matrix. VHN 388. × 250

(c) Six per cent chromium weld metal, as-welded, very fine Widmanstätten alpha precipitate in beta matrix. VHN 452. × 250

(b) Same as above after alpha-beta heat treatment, alpha precipitate coarsened by heat treatment. VHN 304. × 250

(d) Same as above after alpha-beta heat treatment, alpha precipitate coarsened by heat treatment. VHN 335. × 250

Fig. 24 Weld-metal structures in 1/4-in. intermediate alloys. Etchant 1 1/2 HF, 3 1/2 HNO_3, 95 H_2O



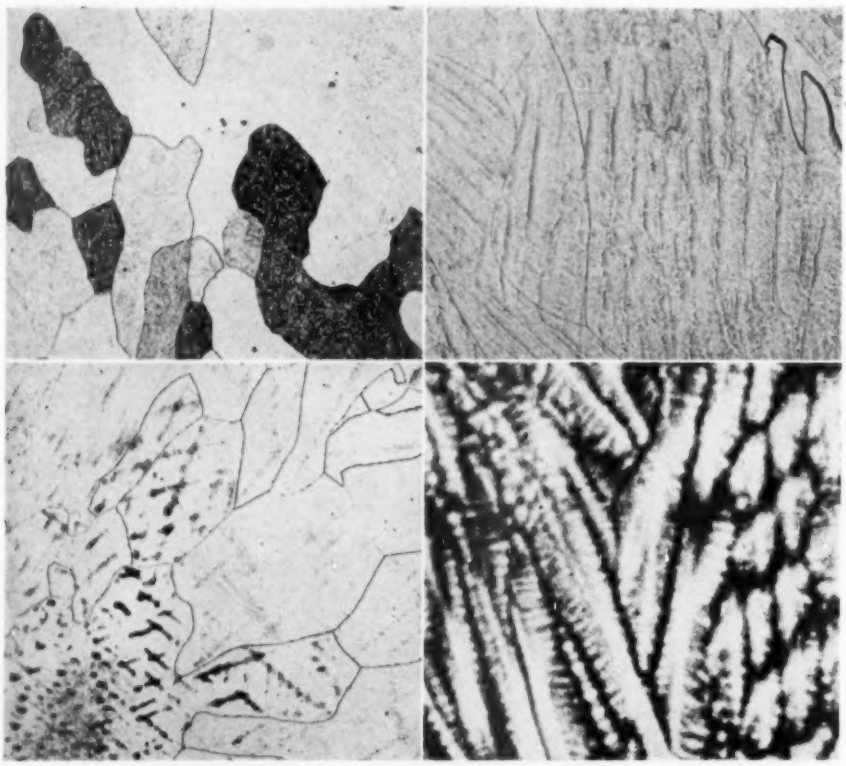
(a) Ten per cent manganese weld metal, as-welded retained beta with network precipitate. VHN 380. × 250

(c) Nine per cent molybdenum weld metal, as-welded, retained beta with network precipitate. VHN 427. × 250

(b) Same as above after alpha-beta heat treatment, alpha precipitated in beta matrix by heat treatment. VHN 353. × 250

(d) Same as above after alpha-beta heat treatment, alpha precipitated in beta matrix by heat treatment. VHN 328. × 500

Fig. 25 Weld-metal structures in 1/4-in. high alpha-beta alloys. Etchant 1 1/2 HF, 3 1/2 HNO_3, 95 H_2O



(a) Thirteen per cent chromium weld metal, as-welded, retained beta with alpha precipitate. VHN 329. × 50

(c) Fifteen per cent molybdenum weld metal, as-welded, cored retained beta structure. VHN 342. × 100

(b) Same as above after beta-quench heat treatment, retained beta with alpha precipitate, showing freezing segregation. VHN 324. × 50

(d) Same as above after beta-quench heat treatment, retained beta with alpha precipitate showing excessive freezing segregation. VHN 313. × 100

Fig. 26 Weld-metal structures in 1/8-in. metastable-beta alloys. Etchant 1 1/2 HF, 3 1/2 HNO_3, 95 H_2O

treated weld metals in the metastable-beta 13% chromium and 15% molybdenum 1/8-in. alloys are shown in Fig. 26. The as-welded structure in the 13% chromium alloy appears to be retained beta with some alpha phase present in the grains. The weld was ductile in this condition. The beta-quench heat-treated specimen of this alloy was brittle. The dark patches observed in the heat-treated structure appear to be caused by alpha precipitation in the alloy lean areas of the weld metals. The dendritic pattern observed indicates that this alloy deficiency in some areas was caused by a freezing mechanism. The microstructure shown for the 15% molybdenum welds shows considerable coring and freezing segregation in both the as-welded and beta-quenched heat-treated conditions.

SUMMARY

The important findings from this investigation are as follows:

1. The bend ductility of welds in the alpha-beta alloys decreased rapidly with increasing alloy content, becoming very low when the alloy content approached 3 to 6%.

2. Postweld heat treatments designed to minimize transformation hardening and to stabilize the alloys may generally be used to improve the bend ductility of welds in alloys having up to 6% alloy content.

3. Weld embrittlement in alloys containing up to 6% alloy content appeared to be correlated with transformation hardening.

4. Welds with very good bend ductility in the as-welded condition were made in the 13% metastable-beta chromium alloy, but the welds were embrittled by heat treatment.

5. Welds in the other high-alpha-beta and metastable-beta alloys had very low ductility in both the as-welded and heat-treated conditions. Since the hardness of the weld metals and heat-affected zones approached that of the base metals after heat treatment, it appeared that factors such as freezing segregation, cast structure or contamination may have contributed also to weld embrittlement.

6. The room-temperature notch toughness of the base metals and ductile weld metals was relatively good for the low and intermediate alloys in all but the iron series of alloys. The notch toughness of the base metals and ductile weld metals was relatively good at −40° F for the low alloys and some of the intermediate alloys.

7. Transformation hardening was observed in the weld metals and heat-affected zones of the welded joints in most of the alloys. It is caused by the structural changes which occur when these zones are heated into the beta field and rapidly cooled during the welding operation. Transformation hardening increased with increasing alloy content in the low and intermediate alloys, reached a maximum in either the intermediate or high-alpha-beta alloys, and with the exception of the iron alloy, decreased considerably for the metastable-beta alloys. In the case of the 15% manganese and 13% chromium alloys, transformation hardening became practically negligible.

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Investigation of Structural Failures of Welded Ships

Samples of fractured plates from 100 ships were examined, and various laboratory examinations and tests were made on 130 plates selected from these samples. Test results are analyzed and reported

*by Morgan L. Williams and George A. Ellinger**

Part I. Examination of Samples and Analyses of the Failures

Abstract

Structural failures of welded ships are analyzed on the basis of laboratory tests of the fractured plates, examinations of the fractures and information regarding the nature and circumstances of the failures. The fractures originated invariably at notches resulting from design details, welding defects or accidental damage, and usually occurred at low temperatures.

Charpy V-notch tests show that plates in which fractures originated were more notch sensitive than plates that did not contain a fracture source. The evidence indicates that the failures resulted from the combined effects of stress concentration at notches and steels that were notch sensitive at the operating temperature.

Relations between the notch sensitivity and the chemical compositions of the plates show that carbon and phosphorus increase the notch sensitivity, and that notch sensitivity is decreased by increasing amounts of manganese and silicon, or by finer grain size.

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*The opinions expressed herein are those of the authors and do not necessarily represent those of the Ship Structure Committee; the Bureau of Ships, Department of the Navy; or the National Bureau of Standards.

INTRODUCTION

Nature and History of the Problem

APPROXIMATELY 5000 merchant ships were built during World War II, and more than one-fifth of them had developed cracks in varying degrees before April 1946, when most of these ships were less than three years old.[1] Some of these failures resulted in the loss of lives and of ships. From November 1942 to December 1952, more than 200 ships have sustained fractures that were classified as serious.[2-4] This number does not include casualties resulting from war damage or from external causes such as grounding or collision. At least 10 tankers and 3 Liberty type cargo vessels have broken completely in two as a result of the propagation of fractures that originated at some point in the hull structure or its appendages (Fig. 1). About 25 other vessels have suffered com-

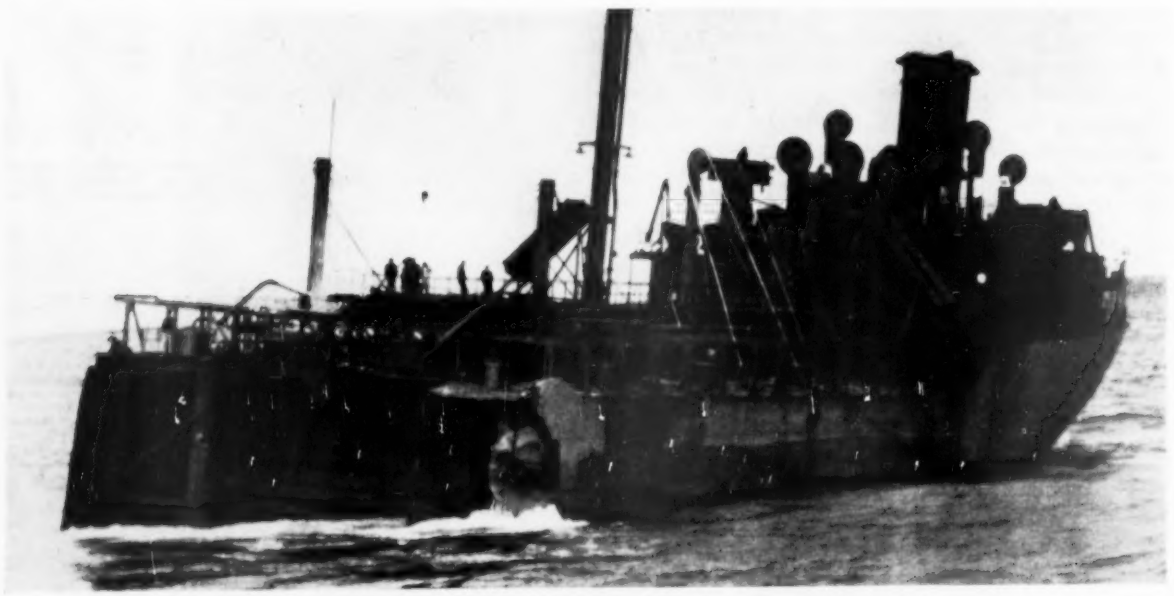

Fig. 1 After-section of a tanker that broke in two at sea, while loaded

Several people were removed from the forward section of this vessel before it capsized. The after-section was salvaged and is in use as a power plant. NBS Ship No. 46, Type T2-SE-A1. (U. S. Navy Photo.)

plete fractures of the strength deck or the bottom.

Although fractures have also occurred in riveted ships and structures,[5] these fractures usually ended at discontinuities introduced by the riveted joints, and did not propagate into adjoining plates. A welded structure, however, is continuous, and a crack may propagate across the welds from one plate to the next, resulting in a complete structural failure. Therefore, the problem of fracture origin and propagation has received much attention in the past few years, because of the great increase in the use of welding in the fabrication of ships and other large structures.

In April 1943, the Secretary of the Navy established a Board to Investigate the Design and Methods of Construction of Welded Steel Merchant Vessels.[1] The Metallurgy Division of the National Bureau of Standards had already assisted in the investigations of some of the earlier failures, and was requested to continue the investigation of the plates removed from the fractured ships. Properly identified samples of the fractured plates were obtained through the cooperation of the U. S. Coast Guard[6] and other interested agencies.

Scope of the Investigation

Samples from 100 ships in which fractures occurred were received and examined prior to Dec. 1, 1952.* In many instances, the sample from one ship included parts of several plates. To coordinate the results of the laboratory investigations with service experience, information was obtained from the Coast Guard and other cooperating agencies regarding the circumstances of the casualties, structural features of the ships involved, location and extent of the fractures and other pertinent details.†

The preliminary studies included (1) determination of the location of the sample pieces with respect to the fracture and to the structure of the ship, (2) visual examination of the fractures and of welds, (3) determination of the starting point of the fracture from this examination or from available reports of the casualty and (4) evaluation of factors which may have contributed to the origin and propagation of the fractures.

When sufficient and suitable material was available, one or more plates from each of the ships were selected for more thorough laboratory investigation. However, in some instances suitable test specimens could not be obtained because the fractures occurred entirely in a weld, the plates were too thin for standard Charpy notched-bar specimens, or the samples were warped or battered as a result of the casualty, or contained welds or flame cuts so located that the properties of the steel might be affected. The laboratory investigation of the plates included detailed examinations of the fractures and the welds, chemical analyses of the plates,* Charpy V-notch impact tests over a range of temperatures, and tension tests. Measurements of the reduction of thickness at the fracture edge were made on plates which were not too badly corroded or battered, microscopic examinations were made on all plates in which fractures originated and on a number of other plates, and gas analyses by the vacuum fusion method were completed on about half of the plates.

DEFINITIONS

As certain terms and the way in which they are used throughout this report may not be familiar to all readers, they are defined or described as follows:

Failure Temperature. The temperature, in degrees Fahrenheit, of the air or water at the time of the casualty under consideration. The water temperature (W) is used for plates below or near the waterline, and the air temperature (A) for plates definitely above the waterline.† The failure temperature for a ship is the temperature at the location of the fracture source.

Classification of Plates with Regard to Fracture Category. On the basis of the information derived from the examinations of the samples and from the reports of the casualties, the plates were classified in three categories, as follows:

1. Source (S): Plates that contained the starting point of a fracture which occurred in service. (This category includes some plates that contained both the starting point and the end or ends of a short fracture, and a few plates in which secondary fractures originated and propagated to meet the main fracture. A number of the fractures originated near welds, usually at locations that involved a structural or geometrical notch as well as the metallurgical effects on the plate metal resulting from the welding operation. However, if the fracture originated *in* a weld due to insufficient weld metal, poor fusion, or slag inclusions, the adjoining plates were not included in this category.)

2. Through (T): Fractured plates that contained neither a fracture source nor a fracture end.

3. End (E): Plates that contained the end of a fracture and did not contain a fracture source. (This category includes three plates which showed definite evidence that the fracture had stopped in the plate, although they were broken completely as a result of increased stress or secondary fractures propagated from other plates as the vessel broke in two. Plates in which the fracture ended at a rivet hole or at an edge of the plate (as at a crack arrestor slot) are not included in this category; these are considered as "fracture through" plates.)

Notch. A notch[1] may be defined as any discontinuity. As used in this report, a notch means a structural discontinuity, such as is occasioned by hatch openings, sheer strake cutouts, foundations, vent openings, bilge keels, the abrupt terminations of structural members, etc., and imperfections in the structure resulting from fabrication, such as peened-over cracks, undercut welds, porosity and inclusions in welds, and incomplete penetration which leaves voids at the center of the joint.

Notch Sensitivity or Notch Brittleness (the Opposite of Notch Toughness). Susceptibility of a material to brittleness in areas containing a notch. Notch sensitivity is characterized by a reduction of nominal strength under impact or static loading, in the presence of a stress concentration.

Notched Bar Test Specimens. The standard Charpy V-notch specimen was used for the notch-sensitivity tests. This specimen is Type A in Fig. 3 of Tentative Standard E23–47T, ASTM Standards, 1952, Part 1, page 1451. Unless otherwise specified, the data refer to specimens cut so that the length of the specimen is parallel to the direction of rolling of the plate (longitudinal) and notched on a side perpendicular to the original plate surface.

Plate. For the purposes of this report, "plate" refers to each separate piece of steel fabricated into a ship. Although most of the pieces were in the form generally defined as plate, the term in this report includes also tubular or curved members formed from plate and a few rolled structural shapes such as channels, bars, bulb angles and half rounds.

Thickness Reduction at Fracture. The difference between the average thickness measured as near as possible to the fractured edge and the average plate thickness an inch or more away from the fracture, expressed as a percentage of the plate thickness.

The Transition Temperature, or 15 Ft-Lb Transition for a Given Plate. The temperature in degrees Fahrenheit at which the notched-bar test curve crosses the level of 15 ft-lb energy absorption. The notched-bar test curve is plotted as energy absorbed vs. temperature of test for longitudinal Charpy V-notch speci-

* A few additional samples were discarded because the material received was not suitable for examination or tests, or because of incomplete information regarding the circumstances of the casualty or the location or identification of the samples.

† The steels for hull construction were purchased under American Bureau of Shipping Specifications. The specification requirements in effect at the time these ships were built,[7] and the current specification requirements,[8] are given in the appendix.

* The average chemical composition of the plates tested was approximately: 0.24% carbon, 0.43% manganese, 0.02% phosphorus, 0.03% sulfur, 0.04% silicon and residual amounts of other elements. Analyses of the majority of the plates tested to date are given in the appendix.

† These temperatures are the best available approximations of the probable minimum temperature of the plates at the time of fracture.

mens, notched perpendicular to the plate surface.

DESCRIPTIONS OF THE FAILURES

Types of Vessels and Classification of Casualties

A summary of the types of ships represented by the 100 samples examined, and of the number and classification of casualties, ages of ships, and temperatures at failure for the various types of vessels is given in Table 1. The vessels in which failures occurred were of several different types, and had been built at a number of different yards. Hull fractures are classified in accordance with the definitions on page 19 of Reference 1, as class 1 (serious), class 2 (potentially dangerous) and class 3 (minor). Table 1 also shows the number of vessels of each type which broke completely in two or suffered complete fractures of the strength deck or the bottom (both included in class 1 failures). The hull structure was not involved in four of the casualties: A rudder tube and a crack-arrestor strap on Liberty Ships, Type EC2-S-C1, and two mast failures in Victory Ships, Type VC2-S-AP2.

Five of the casualties probably resulted from damage by ice or floating objects—three from minor collisions with a tug while docking, two from possible grounding and one from an underwater explosion. The other casualties occurred under normal operating conditions, or in a few cases, during building, alterations or drydocking. Three of the ships were of riveted construction: two Coast Guard Cutters in which plates were cracked while navigating in ice, and a 31-yr-old collier that broke in two during a severe storm.[5] The original hull of another ship was riveted, but the fractures occurred in the welded superstructure during conversion.

Two ships, both T2 tankers, which had suffered separate casualties at different times, and at different locations in the vessels, were counted twice in this investigation. In a few other instances, a sample included material from separate casualties of the same ship. These were considered as a single casualty, and were given the same NBS ship number.* The records indicate that several of the vessels included in this investigation have suffered previous or subsequent casualties, but samples representing these failures were not obtained. Previous failures are not considered in this report except as they might influence the failure under investigation, for example, a failure of, or resulting from, a previous repair.

The age of the welded ships at the time of the casualties ranged from one minute (after launching) to more than 10 years. A few of the cracks occurred during construction, before or after the ship was launched. Most of these vessels were built during the period of World War II, and the majority were less than three years old when the failures occurred. The average age of the Liberty Ships (Type EC2-S-C1) was somewhat less than for most of the other types, probably because of the large number of early failures resulting from structural features that have since been modified. The relatively few riveted ships were 10 to 31 years old.

The temperatures at failure are the air or water temperatures that were reported at the time the failures occurred. Temperatures reported at the time a fracture was found were disregarded unless there was some reasonably definite evidence that the fracture actually occurred at the time, and not previously.

* The "NBS ship number" assigned to each vessel does not correspond to the ship numbers used in some of the references,[1–3] and is significant only for identification of the samples received and plates tested at this laboratory.

Table 1—Summary of Ship Types, Classification of Casualties, Ages of Ships, and Temperatures at Which Failures Occurred

Type of Vessel	Total Number of Ships Examined	Number and Classification of Casualties						Ages of Ships		[c]Temperatures at Failure		
		[a]Broke in Two	[a]Deck or Bottom	Class 1	Class 2	Class 3	Other (Note b)	Months Afloat Average	Range	Average °F	Range °F	No. of Ships [d]
A. Liberty Cargo Ships EC2-S-C1	30	1	2	13	8	7	2	23	0-99	40°	23°-58°	(18)
B. Tankers T2-SE-A1 & A2	27	7	1	19	7	1	-	53	2-96+	46°	30°-67°	(21)
T1-MB-T1	1	1	-	1	-	-	-	41	(1 only)	35°	—	(1)
Tanker, Not MC Type	5	-	1	5	-	-	-	58	9-122+	44°	42°-48°	(3)
Total Tankers	33	8	2	25	7	1	-	54	2-122+	45°	30°-67°	(25)
C. Miscellaneous Types C2-S-E1 and Mod C2	9	-	1	5	4	-	-	63	21-100	43°	27°-60°	(6)
C3-S-A2	5	-	1	3	2	-	-	62	17-110	52°	46°-56°	(3)
2-ET1-S-C3	5	-	-	3	2	-	-	31	4-75	40°	24°-50°	(3)
VC2-S-AP2	5	-	-	2	1	-	2	57	7-95	35°	20°-54°	(4)
USCGC	3	-	-	-	2	1	-	128	55-207	32°	32°	(3)
Rivetted Collier	1	1	-	1	-	-	-	377	(1 only)	32°	—	(1)
All Other Types	9	-	1	3	3	3	-	73	0-300	17°	0°-60°	(5)
Total Misc. Types	37	1	3	17	14	4	2	74	0-377	35°	0°-60°	(25)
Total All Types	100	10	7	55	29	12	4	52	0-377	40.5°	0°-67°	(68)

a. Number of vessels which broke completely in two or suffered complete failures of deck or bottom strength members, included in Class 1 (serious) casualties.
b. Includes one rudder tube and one crack arrestor strap (EC2-S-C1), and 2 mast failures (VC2-S-AP2).
c. Air or water temperature, depending on location of fracture source above or below waterline.
d Temperature at time failure occurred is not known for some cases, as for example when fracture was found in drydock or after discharge of cargo.

Most of the casualties occurred at low temperatures. The highest temperature at which a fracture is definitely known to have started is 67° F, and the lowest, 0° F. The majority of the fractures started at air or water temperatures between 30 and 45° F, and the few which occurred at higher temperatures generally did not propagate extensively. No relation has been found between the ages of the ships and the failure temperatures, even when ships of the same type were considered. There appears to be no significant difference of failure temperatures for ships of different types.

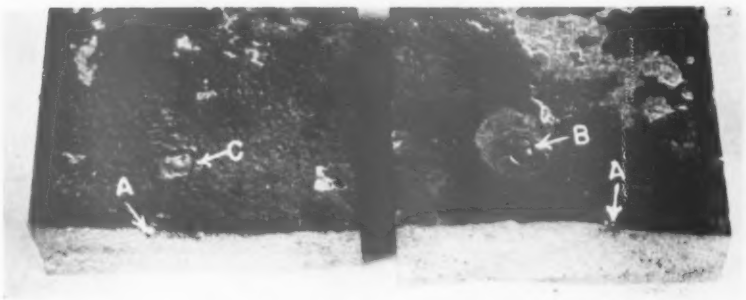

Fig. 2 Piece of ship plate fractured in the laboratory

Fracture source is at intentional arc strike (arrows *A*), similar to fracture sources in a ship as shown in Fig. 12. Arrows *B* and *C* indicate arc strikes with small deposit of weld metal. Note that the fracture source is at the smallest crater. × 1.

Origin and Propagation of the Fractures

A ship afloat may be considered as a hollow beam or box girder subjected to complex and variable forces, which are further complicated by stress concentrations resulting from the numerous structural discontinuities that are necessary to the function, operation or construction of the vessel. At sea, the structure may act as a bridge between two wave crests, or as a seesaw balanced on the crest of a single wave. Thus the stresses, especially in the deck and the bottom, may vary in a short time from maximum compression to maximum tension. These dynamic stresses are added (algebraically) to the more or less static stresses such as those resulting from the distribution of the weight of the vessel itself and its cargo, from unequal thermal expansion due to temperature differences, and probably also from residual stresses caused by fabricating operations or accidental damage. The fractures undoubtedly occurred under conditions such that additive tensile stresses, magnified by the effects of stress concentration, exceeded the fracture strength of the steel. Relatively little was known, when these ships were built, about the magnitude of the stresses and stress concentrations existing in welded ships under various operating conditions. Extensive studies in these fields have been conducted during the past decade,[9] and have indicated that stress concentration factors of 2 to 3, and even as high as 4.6 may occur at structural discontinuities.

The weather and sea conditions at the time of the casualties ranged from calm, in port, to heavy storms at sea. From the circumstances reported in some of the cases, it appears that the stresses existing at the time of the casualty must have been almost entirely static. However, in the majority of the failures, dynamic forces resulting from wind and wave action, from inertia loads caused by the pitching of the vessel and from the interaction of elastic waves in the steel could have contributed large and variable components to the stresses existing at the points where the fractures originated.

Stresses resulting from unequal thermal expansion in different parts of the hull structure due to large differences between the air and the water temperature, solar radiation on part of the vessel, refrigerated cargo spaces or heating of the cargo oil may have been contributing factors in several of the failures.[10, 11]

The loading and distribution of the load in the vessels was undoubtedly a factor contributing to some of the casualties, although in several ships in which fractures occurred the calculated average stress in the deck or the bottom was less than 12,000 psi, or roughly one-third the yield point of the steel.[1] Fractures originated in the deck area in six of the tankers included in this report (NBS ship Numbers 11, 36, 46, 47, 52 and 61). Five of these ships broke completely in two, two of them while in port in relatively still water. It was noted that in all of those ships the cargo tanks were light. This resulted in a bending moment that caused a tensile stress in the deck as illustrated by the positions assumed by the two parts of the broken tanker shown in Fig. 15. The remaining 26 Class 1 and 2 casualties in tankers (including three ships that broke in two) occurred when the vessels were loaded, and all of these fractures originated in the bottom or bilge strakes, with the exception of one in the forepeak (NBS ship No. 74). The relative bending moments of a loaded tanker and of one that is light may be visualized by comparing Fig. 1 (loaded) and Fig. 15 (light).

The starting points of more than half of the fractures of known origin were in the immediate vicinity of structural features such as hatch corners, ladder cutouts or other openings, or at the abrupt termination of stiffeners such as a bilge keel, longitudinal, doubler plate or parts of the superstructure. The origin of the fractures at these points may be attributed primarily to the stress concentration resulting from a geometrical or structural notch, although in nearly every case the metallurgical effects of welding, flame cutting or mechanical working were also present, and these effects are not separable.

Another third or more of the fractures originated in defective welds, some associated with structural notches, and others in which the only notch present was that resulting from the welding defects. A few fractures started near interlocking welds such as at points where a pad or doubler was welded on top of a seam or butt weld, or where two weld beads were laid so close together that a small mechanical notch was formed between the welds. In both of these conditions, the heat-affected zones of two welds overlapped or joined. The origin of these fractures probably resulted from the combination of a minor mechanical notch and the metallurgical effects of the successive heating and cooling cycles.

Several of the fractures originated at arc strikes or craters formed by striking a welding arc on the plate outside of the weld zone; or near small shallow welds on heavy plate. As some of the features of the arc strikes in the ships were obliterated by the effects of corrosion, an example for illustration was prepared in the laboratory. Intentional arc strikes and small weld beads were made on a piece of ship plate; the piece was cooled in contact with dry ice (CO_2), and broken with a hammer blow while supported as a simple beam. The piece fractured without measurable bending, and the origin of the fracture was at a small arc strike, as shown by arrows *A* in Fig. 2. The appearance of the "fisheye" at the origin of the fracture, immediately under the crater, is similar to those observed in the ship fractures, such as those illustrated in Figs. 11, 12 and 17. A companion piece, tested without arc strikes or welds, withstood several blows and bent without fracture. This experiment demonstrates that an arc strike is an effective notch, and that failures originating at such defects, as observed in the ship failures, may be reproduced in the laboratory.

Figure 3 shows a longitudinal cross section through the arc strike and small deposit of weld metal indicated by arrow *B* in Fig. 2. The difference of the structure of the metal immediately under the crater is an indication of the metallurgical transformations that have occurred. Under the conditions imposed by an arc strike or a small weld, the temperature is raised to the melting point in only a small portion of the total mass. In other

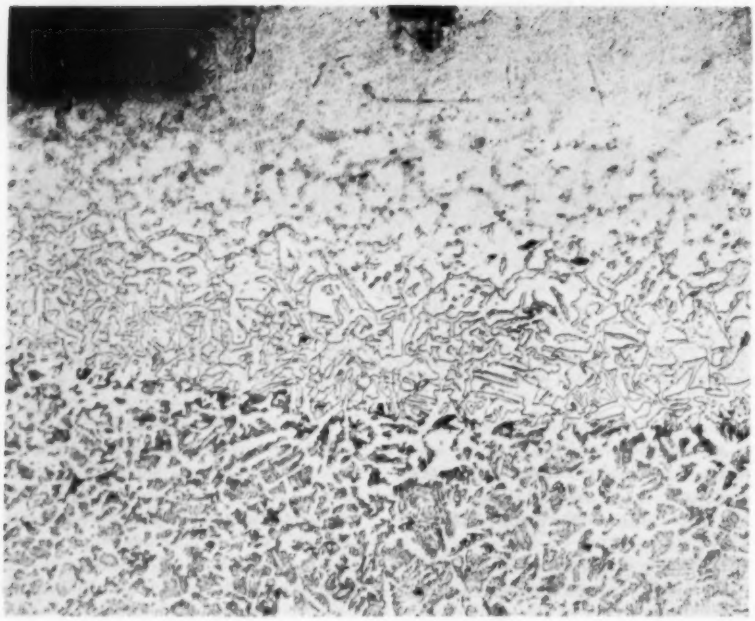

Fig. 3 Cross section through arc strike and weld metal deposit shown by arrow B in Fig. 2

Note the defects and voids in the weld metal (*top*), and the difference of structure of the heated zone (*center*) and the unaffected-base metal (*bottom*). × 75.

words, this heating effect is highly localized, thus the relatively large mass of surrounding metal serves to conduct the heat away very rapidly. This amounts, in effect, to a drastic quench, which causes changes in the metallurgical structure of the base metal in the heated zone, as shown in Fig. 3. The resulting structure is hard and brittle, and in a state of tensile stress caused by restrained thermal contraction and volume changes that accompany the metallurgical transformations. This may lead to the formation of small cracks, but even if cracks are not formed immediately, the condition constitutes a "metallurgical notch" that may be as effective as a mechanical notch in contributing to the origin of a fracture.

Examinations of Fractures and of Welds

Usually the fractured edges of the samples were badly rusted, and some cleaning was necessary before a detailed examination could be made. Loose particles of rust were removed with a stiff bristle brush, and suitable solvents such as acetone or carbon tetrachloride were used to remove the oily residues with which many of the plates were coated. To remove the remaining rust deposits, the fracture edges were soaked and scrubbed with a saturated solution of dibasic ammonium citrate, $(NH_4)_2HC_6H_5O_7$, in warm water. Vigorous scrubbing with a stiff bristle brush (often continued for several hours) was necessary to remove all traces of rust so that satisfactory photographs of the fracture edges could be obtained. This solution does not act as rapidly as some other rust removers, but it does not etch the underlying steel surface to a serious extent. After cleaning, the specimens were rinsed with tap water, sprayed generously with alcohol and dried immediately in an air blast.

The fractures were predominantly of a brittle type, characterized by a break nearly perpendicular to the plate surfaces and a very small reduction of thickness (usually less than 2 or 3%) at the fracture edge. Generally the paint or scale on the plate surfaces was not cracked, even very near to the fracture. This shows that the fractures had propagated with very little plastic deformation of the steel, and that the stress at points a small distance from the fracture must have been lower than the yield point. It is evident therefore that very little energy was absorbed in the propagation of these fractures. In the immediate vicinity of the fracture, however, the sharp notch at the head of the advancing crack caused a stress concentration which exceeded the fracture strength of the materials.

The surfaces of these brittle fractures were rough and usually bore characteristic markings, which have been described as chevrons, herringbones, or arrowheads, as shown in the fracture of the bulwark plate at the right in Fig. 4, and in several of the following photographs. This type of fracture was observed and reproduced at the National Bureau of Standards about 15 years ago, in tests conducted to determine the source of a brittle failure of an aircraft part. These tests, which were discussed in the first NBS report on the examination of steels from a fractured ship,[12] showed that a brittle failure may be produced in a normally ductile metal by applying a tensile stress to a notched-plate specimen. The tests also established the fact that the chevrons point back toward the origin of the fracture.* This made it possible to locate the sources of the fractures in the ships, as for example, in Fig. 4, in which the chevrons point to a faulty butt weld in the bulwark cap rail at the top.

A small percentage of the fractures were of the shear type, having some re-

* An exception to this rule is noted by Kies, *et al.*,[13] in that reversed chevrons may be produced by side scratches, which promote initiation of fracture centers near the surfaces rather than in the center of the plate.

Fig. 4 Saddle weld in a bulwark cap rail

The chevrons (or herringbone markings) in the fracture of the bulwark plate at the right point toward the source of the fracture in the defective weld of the cap rail. Fracture starting at this and two similarly defective welds in the cap rails propagated to cause a complete failure of the strength deck. NBS Ship No. 37, Type C2-S-E1. × 0.7.

duction of thickness near the fracture, and usually showing evidence of ductility by the cracking of the paint and scale for some distance from the fracture. The starting points of the fractures were always of the brittle type, showing no macroscopic evidence of shear, but in several instances brittle fractures changed to the shear type, and in a few of the plates alternating shear and brittle areas were observed. The shear type fractures were generally found in thin plates and were usually associated with the free edge of a plate, the presence of rivet holes or openings, deformation of the plate or the ending of a fracture. Such fractures were not found in plates more than $^{5}/_{8}$ in. thick except in one case, a deck plate (NBS plate No. 46c, $^{13}/_{16}$ in. thick) which had a shear type fracture intersecting a brittle fracture at nearly a right angle.

Measurements of the thickness reduction at the fracture edge were made when the condition of the sample permitted reasonably accurate determinations. Nearly all of the recent samples were so badly corroded and pitted that accurate measurements could not be made, and many of the other samples did not contain a fracture edge, or the fracture was battered, chipped or welded. The thickness reduction at the brittle fractures ranged from 0.4 to 4%, the higher values generally being associated with plates in which fractures ended. Reductions of more than 5% were found at the shear fractures. Most of the shear fractures were of the single shear type, and the reduction in the actual fracture area was undoubtedly greater than the minimum value determined by measurements at the base of the 45-deg fracture. Thickness reductions of about 30% were found in two small areas of double shear fracture in NBS plate 56B (see Fig. 20).

Many of the fractures classified as brittle type showed narrow zones of shear or shear lips adjacent to the plate surfaces, which probably contributed materially to the total thickness reductions in these cases. For example, on Plate 17C the shear lips were at least 0.02 in. wide (0.04 in. for both sides of the plate). The plate thickness was 0.64 in. and the average thickness reduction was 0.02 in. or 3.1%. If we assume a thickness reduction of 25% in the zones of shear fracture, this would account for 0.01 in. or half of the total thickness reduction, and the thickness reduction in the brittle part of the fracture would then be 0.01/0.60, or 1.7%.

Some of the plates were not fractured completely through in a few small areas, giving the appearance of an intermittent fracture, and occasionally cracks several inches long were found on one side of a plate, with no visible indication of a crack on the other side. When these fractures were broken apart for examination, it was found that only a thin skin or shell at the plate surface had not been fractured, corresponding to the zones of shear fracture observed near the surface in many of the plates which had fractured completely.

The shear lips, and the occasional absence of fracture near the plate surfaces, may be attributed to a combination of two factors: greater notch toughness near the plate surface, especially in rimmed steels, and decreased constraint or biaxial stress near the plate surfaces.

Figure 5 illustrates the cracking of the paint near the end of a shear fracture, which provides an indication of the strains in the vicinity of a crack. In the seam weld shown in the photograph, the fracture changed from the brittle to the shear type, apparently because of superior properties of the weld metal, and the shear fracture halted in the plate below, less than $^{1}/_{2}$ in. from the seam weld. The cracking of the paint indicates that this plate (NBS plate 60A) was deformed for several inches ahead of the end of the fracture, and for at least an inch on each side of the fracture. Near the brittle fracture in the plate above, however, the paint was not cracked (except for a small area immediately adjacent to the weld, and for the random cracking caused by crazing of the paint film) indicating that the brittle fracture had propagated at a strain less than that required to crack the paint.

Fig. 5 Strain at the end of a shear fracture shown by cracking of the paint on the inboard side of the plate

A brittle fracture, propagating from above, changed to the shear type in the seam weld, and ended in the lower plate (NBS Plate No. 60A) $^{1}/_{4}$ in. below the weld. In the plate above, the strain adjacent to the brittle fracture was not sufficient to crack the paint. × 0.6 approximately.

A typical end of a brittle fracture in ship plate is shown in Fig. 6. The original crack had rusted, and appears dark in the photograph. The lighter portion at the right was broken in tension in the laboratory. Chevrons, pointing to the left, could be seen in both the old and the new

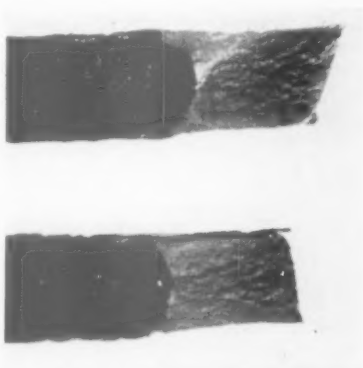

Fig. 6 Typical end of a fracture

The lighter portion at the right was broken in tension in the laboratory. The original crack had progressed farther at the center of the plate than at the plate surfaces. × 1.

portions of the fracture. The original fracture had progressed somewhat farther at the center of the plate than at points nearer to the plate surfaces, resulting in a semicircular or oval shape of the fracture end. Similar oval shapes, of varying symmetry and convexity, were observed at every fracture end which was examined and in every case the fracture was less advanced at points near the plate surfaces. The chevrons were approximately perpendicular to the oval shapes of the fracture ends, suggesting that the chevrons were probably the traces of irregularities in the advancing fracture front. Kies, Sullivan and Irwin[12] have shown that chevron markings may be produced in plastics, and that they may be attributed to level differences of fracture centers which are initiated in the vicinity of the leading edge of the main crack.

It was also noted that the apex angles of the chevrons were not the same in different plates, or even in different parts of the same plate, but the cause of these variations is not definitely known at present. The experiments cited in the previous footnote and in Reference 13 suggest that this phenomenon may be related to differences in the degree of biaxiality in the plate, which would change the concentration of fracture initiation centers. However, it might also be related to the speed of propagation of the fracture, the stress level at various locations along the path of the fracture, the notch sensitivity of the steel or nonhomogeneity of notch sensitivity in the thickness direction.

Semicircular or oval markings, similar to the shape of the end of a fracture, were observed in several of the fractured plates. Often these marks were accompanied by a difference in the degree of corrosion of the fracture edges, a change in the direction of the fracture or a slight indication of shear type fracture, indicating that the crack had stopped temporarily at that point and had progressed further at a later time or under different stress conditions.

The fractures in welds* occurred in regions where the strength of the weld was reduced by subsurface defects which introduced internal notches or which seriously decreased the effective cross-sectional area of the weld, as shown in the butt weld of the bulwark cap rail at the top of Fig. 4. Usually these defects were not visible on the surface, and would not be detectable by ordinary surface inspection methods. In all of the fractured welds that were examined, very evident welding defects were found, such as slag inclusions or porosity, poor fusion, or insufficient weld metal due to inadequate joint preparation or lack of penetration. When the fracture encountered a sound area of the weld, it turned into one of the adjoining plates or, in a few instances stopped. Usually it was not possible to determine the exact starting point or the direction of propagation of a fracture in a weld, as these fractures did not show the herringbone markings or chevrons characteristic of the brittle fractures in plates.

* Fractures parallel to a weld, as distinguished from fractures which propagated across or through a weld from one plate to the next.

Summary and Evaluation of Observations

The lack of ductility and the brittle nature of the majority of the fractures indicated that when the steel was incorporated in the structure of the ship, the mechanism of fracture, or the mechanical behavior of the steel, was not the same as when determined by the usual tensile test, using relatively small specimens. This phenomenon is similar to that observed in tests of notched specimens in tension or bending, particularly at low temperatures. The similarity is also evident in the facts that the fractures in the ships occurred more frequently at the lower operating temperatures, and that the starting points of the fractures could be traced, invariably, to geometrical or metallurgical notches resulting from structural or design details, fabrication processes or defective welds. This phenomenon, called notch sensitivity or notch brittleness, is not peculiar to ship plate alone, and is not confined to metals. The scoring of glass for cutting and the notching of cellophane wrappers are familiar examples in which notch sensitivity is utilized to control the location or direction of a tear or fracture.

The most serious aspect of notch sensitivity, however, is not the fact that fractures may start at structural notches, but that in a notch-sensitive steel a crack, once started, will continue to propagate at a very low energy level. Realization of this fact, together with the observation that in riveted ships the fractures usually stopped at a riveted joint, led to the installation (in vessels already built and in new construction) of riveted straps covering a longitudinal slot in the deck, the bottom plating or the sheer strake, as a preventive measure to limit the propagation of fractures. The effectiveness of these crack arrestors[1-3] shows quite conclusively that the propagation of the fractures was not a direct result of the loss of the strength and support of the structural members initially fractured, but was due primarily to notch sensitivity. Propagation of fractures was halted by the crack arrestors in nearly every casualty of vessels so equipped, and in the few remaining cases it was reported that the crack arrestors delayed the propagation of the fractures. Thus, even in vessels where almost the entire deck or bottom was fractured, the remaining structure was able to absorb enough energy to prevent the immediate start of a new fracture on the other side of the crack arrestor. However, in the presence of the severe stress concentration caused by the initial crack, the fractures could propagate at relatively low over-all stress and energy levels until the continuity of the metal was broken by the crack arrestor slot.

As stated previously, the more serious casualties in welded ships resulted from the propagation of fractures through or across the welds into adjoining plates. However, in a number of cases far out of proportion to the relative areas of welds and plate metal, the fractures stopped at or near welds which were perpendicular, or nearly perpendicular, to the path of the fracture. In several of the ships, it was noted that fractures appeared to avoid the areas of welds and heat-affected zones but propagated 3 to 6 in. from a butt weld and generally parallel to it. Some of these fractures turned and crossed the weld at a rather sharp angle then turned again and propagated in the adjacent plate parallel to the weld. No instance can be recalled in which a fracture propagated for any distance in the heat-affected zone adjacent to a weld, or continued to propagate in a sound weld after turning from a plate into a weld. This indicates that sound weld metal and the metal in the heat-affected zones adjacent to a weld are not abnormally sensitive to fracture in a direction parallel to the weld.

Fractures of the shear type were relatively more numerous in appendages such as longitudinals, free bulwark plates or corrugated bulkheads than in the shell plates. This (together with the association of shear fractures with thin plate, deformation or buckling, the free edge of a plate or the presence of openings) suggests that shear fractures are more likely to occur in members which are free to deform laterally, and that the brittle fractures are associated with lateral restraint. This constitutes further indirect evidence that the brittle, low-energy fractures are associated with notch sensitivity, since lateral restraint, and restraint in the thickness direction of thicker plates, may impose conditions of biaxial and triaxial stress similar to those which occur at the root of a notch.

However, the failures occurred in vessels of several different types, and at various locations in these vessels, while there were no fractures in the majority of other ships of the same design and construction. It may be that some ships fractured while others did not because the lack of notch toughness of the steel was a borderline deficiency, as, over a period of years, the operating conditions would presumably impose similar combinations of temperature and stress concentration at corresponding locations in all ships of each type. This supposition is supported by the results of the laboratory tests, discussed in Part 2 of this report.

Examples of Typical Failures

It is unfortunate that ship construction

Fig. 7 Fracture originating at a hatch corner of a cargo vessel, NBS Ship No. 32, Type C3-S-A2

Fractures originating at both the port and the starboard hatch corners in this ship resulted in a complete failure of the strength deck. Two sister ships had almost identical failures on the starboard side. (U. S. Coast Guard Photograph.)

requires large amounts of welding at points of geometrical discontinuity, as in many cases the welding was blamed for a failure that was primarily a result of stress concentration at a notch created by structural design. Although some fractures originated in defective welds, many started at structural notches in which welding was not the primary contributing factor. Typical examples of fracture sources, some that resulted from welding operations and others in which welds were not directly involved, are shown in Figs. 7 to 19, inclusive.

A fracture originating at a hatch corner is illustrated in Fig. 7. On this ship similar fractures originated in the $1^1/_4$-in. deck insert plates at both the port and starboard after corners of No. 3 hatch, resulting in a complete failure of the strength deck. The port fracture extended across the deck and into the side plating, and the starboard fracture ended near the edge of the deck, at a bollard that acted as a reinforcement. The hatch beams and coaming, and the deck between the hatch openings, did not fracture except for a few inches near the hatch corners. In two sister ships, NBS ship numbers 70 and 79, almost identical fractures occurred on the starboard side, but in these vessels the port side did not fracture, although small cracks were found at the port hatch corners.

Figure 8 shows a small crack that started at a sharp corner in a ladder opening of a Liberty ship. Similar cracks were found in both the port and the starboard ladder openings of this vessel, and although they had progressed only about 1 in. when discovered, they did form severe notches at critical locations, which might have led to more serious failures. The openings had been made by hand torch cutting, and subsequently dressed by grinding. However, several deep notches still remained on the edge. The fracture, indicated by the arrow, was located at a sharp change in section, which was further aggravated by rough notches resulting from the flame-cutting operation, and probably also by the metallurgical effects resulting from the heating and cooling.

A faulty butt weld in a hatch side facing channel is shown in Fig. 9. Again (as in the weld shown in Fig. 4), the weld metal did not penetrate to the full thickness of the joint—the original flame-cut edge may be seen in the center of the photograph. The upper side of the weld had cracked at some previous time, probably during or shortly after the welding. When the part was painted the crack was covered over, and some paint penetrated the crack, as shown by the light areas near the top of the weld. After this vessel was launched, but before it was completed, there was a sudden complete failure of the strength deck, starting at this joint and at a similar faulty weld on the opposite side.[14] This costly failure might have been averted if, by better supervision and inspection, the initial crack in one side of the weld had been found and repaired before painting.

Fig. 8 Short crack originating at sharp corner in flame-cut edge of ladder opening in sheer strake. NBS Ship No. 9, Type EC2-S-C1. × 1

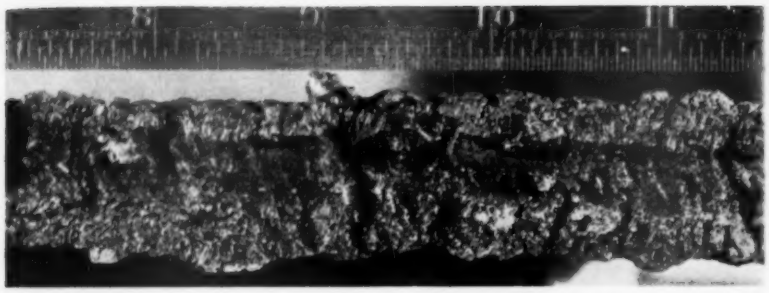

Fig. 9 Faulty butt weld, showing poor weld penetration, in hatch side facing channel, NBS Ship No. 2, Type L6-S-B1

Dark areas at center are original flame-cut edge of plate. Paint penetration in upper part of weld (light areas) indicates weld was partly cracked before painting. Subsequent failure of weld led to complete fracture of strength deck. × 1.

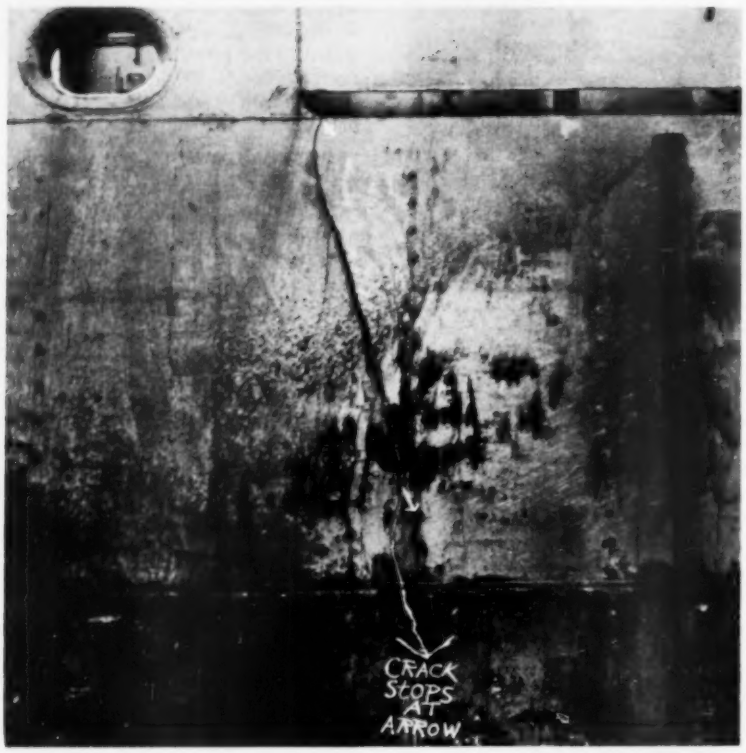

Fig. 10 Fracture source (arrow) at change of section at end of slotted freeing port. NBS Ship No. 21, Type Z-ET1-S-C3. (U. S. Navy Photograph)

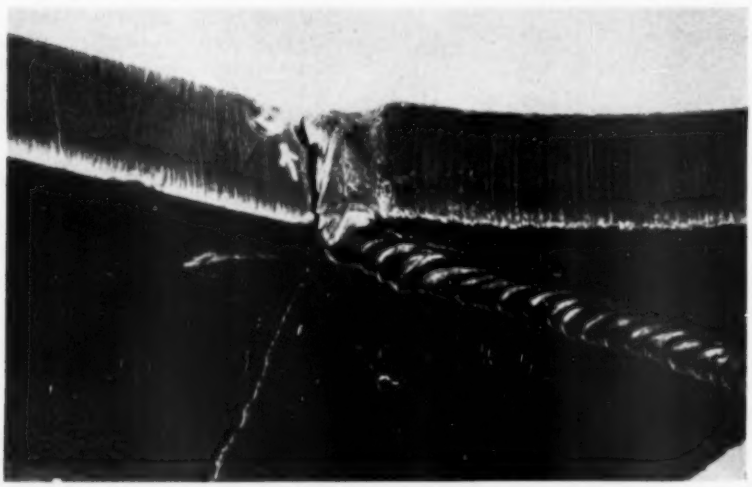

Fig. 11 Source of fracture shown in Fig. 10, viewed from inboard and above. Arrow shows arc crater and globule of weld metal on flame-cut edge of sheer strake plate, adjacent to toe of fillet weld

Figures 10 and 11 illustrate another fracture starting at a scupper or cutout above the sheer strake. The source of this fracture (Fig. 11) was at a sharp change in section at the toe of a fillet weld between the sheer strake (below) and the end of a small triangular insert plate (above). A small crater and globule of weld metal (arrow), probably formed by momentary touching of the welding electrode to the plate, was adjacent to the crack. The heat-affected zone under this globule was quite shallow and was about the maximum hardness for this type of steel (Knoop No. 400), indicating very rapid cooling. The edge of the sheer strake plate at this point had been prepared by machine gas cutting. The energy absorption of a notched-bar test specimen taken from a point very close to this edge was only 60% of the average energy absorption at the same temperature (70° F) of similar specimens from the interior of the plate. This indicates that the notch sensitivity of the steel, which was already high, had been increased in the region of the flame-cut edge by the heating and cooling during the cutting operation. The origin of this fracture was attributed to a combination of factors: (1) a notch-sensitive steel, with the notch sensitivity at the edge of the plate further aggravated by heating incident to the flame cutting; (2) low temperature (45° F), which also increased the notch sensitivity; (3) stress concentration at a geometrical notch or sharp change in structural section; (4) a metallurgical notch near the geometrical notch; and (5) rough seas and a strong wind causing unusual stresses in the structure of the ship.

In Fig. 12, the chevrons indicate that there are two fracture sources in the plate near a lap seam weld. These fractures occurred in the shell plating of a Coast Guard Cutter while navigating through an ice pack. Both sources are at small craters where apparently the welder had struck his arc on the plate, away from the area of the weld, and with practically no deposit of weld metal. The origins of these fractures may be attributed to the combination of a metallurgical notch, the small mechanical notch formed by the arc crater, stress concentration resulting from the overlap of the plates, the metallurgical effects of the nearby weld and low temperature.

Fig. 12 Shell plating of Coast Guard Cutter (NBS Ship No. 14) damaged by ice. Chevrons on fractured edge indicate two fracture sources (arrows) at arc strikes on plate, near lap seam weld. × *0.75*

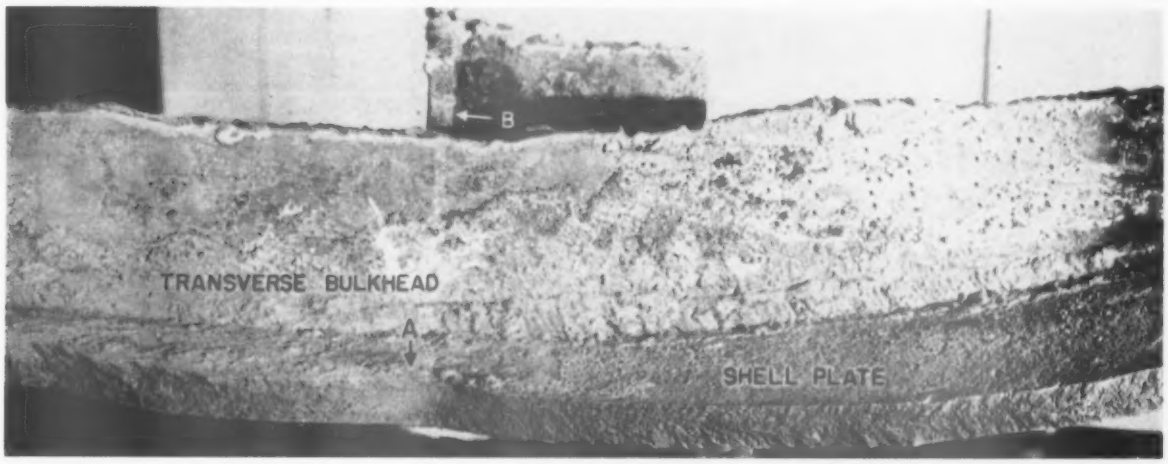

Fig. 13 Source of fracture (arrow A) in shell plating of a tanker that broke in two at sea

NBS Ship No. 8, Type T2-SE-A1. Fracture source is about 2½ in. from transverse bulkhead, exactly at the end of an interrupted longitudinal stiffener. Arrow *B* indicates the interrupted longitudinal on the other side of the bulkhead.

A fracture in the shell plating of a tanker that broke in two at sea is shown in Fig. 13. The chevrons on the fractured edge clearly indicate the source of the fracture in the shell plate, at the point marked by arrow *A*. This point, about 2 ½ in. from a transverse bulkhead was exactly at an end of a longitudinal stiffener. The end of the corresponding longitudinal on the other side of the bulkhead is indicated by arrow *B* in the photograph. The longitudinals here were interrupted for a space of about 6 in. to allow for insertion of the transverse bulkhead, and were connected to this bulkhead, at a point several inches from the shell plating, by gusset plates on the flanges of the longitudinals, as shown (in another ship) in Fig. 14. The longitudinals were rigidly connected to the shell plating by fillet welds all around. The ends of these longitudinals were not rounded or tapered, but were cut square and perpendicular to the shell plating. This condition constituted a structural notch at the abrupt end of the longitudinal stiffener. Probably the majority of failures in tankers originated at this design detail, which is repeated several hundred times in the structure of a T2 tanker.* Another prolific source of failures was at the end of a bilge keel, which provided a similar geometrical configuration.

Figure 14 shows two overlapping secondary fractures in the side shell of a tanker. Both fractures originated at longitudinal interruptions like that shown in the preceding figure, and there were a number of other secondary fractures starting at similar points. In another tanker (NBS Ship No. 81), nine such cracks were found, each of which had started in the shell adjacent to the end of a longitudinal.

A tanker that broke in two while in port is shown in Fig. 15. The positions assumed by the two parts of the vessel are an indication of the bending moment that contributed to the initiation and propagation of the fracture. Figure 16 shows the area in which the fracture originated, in the starboard deck stringer plate just forward of the chock base near the midship section. The mating edges of the fracture, near the starting point, are shown in Fig. 17. The fracture source was at the toe of a small fillet weld

Fig. 14 Overlapping secondary fractures in side shell of a T2 Tanker, NBS Ship No. 56

Both fractures originated at the ends of interrupted longitudinals near a transverse bulkhead. Triangular gusset plates on the flanges of the longitudinals were torn out of the bulkhead (extreme left). (U. S. Coast Guard Photograph.)

* Several variations of this design detail have been tested in the Mechanics Division of the National Bureau of Standards,[3] to evaluate the effectiveness of practical modifications which have been or may be used in existing ships or in new construction.

joining a clip to the deck stringer plate. The clip (for a wartime countermeasure against mines) was probably attached after the ship was built, and was so placed that the toe of the weld was almost superimposed on the weld at the forward end of the inboard leg of the chock base. At the point of origin of the fracture, between these two welds, there were two small arc craters in the deck plate. (The difficult welding position due to the proximity of the chock base probably contributed to this defect of workmanship.) The craters were partially bridged over with weld metal which was not completely fused to the plate, and which formed the projecting lip on the fracture, visible at the toe of the weld in the lower photograph of Fig. 17.

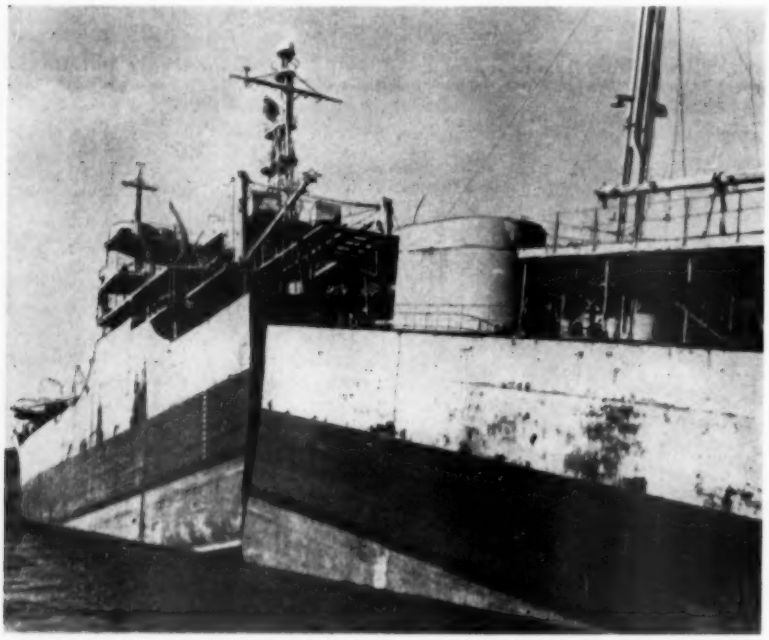

Fig. 15 Tanker that broke in two at dock, viewed from port side, looking forward

The bending moments that contributed to the origin of the fracture are indicated by the positions of the two parts of the vessel. NBS Ship No. 52, Type T2-SE-A2. (U. S. Coast Guard Photograph.)

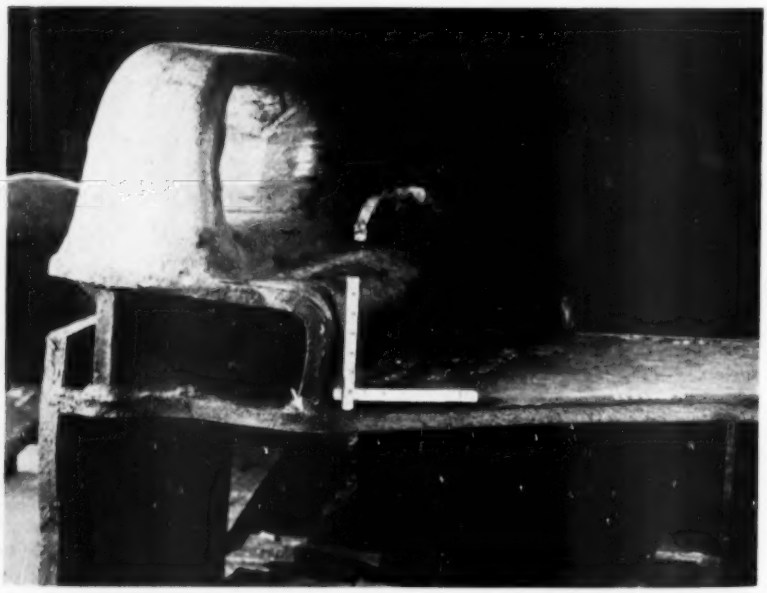

Fig. 16 After edge of fracture in starboard sheer strake and deck stringer of the tanker shown in Fig. 15

The chevrons show that the fracture started in the deck stringer near the inboard leg of the chock base, and propagated into the sheer strake (*left*) and the deck longitudinal and bracket (*lower right*).

The origin of this disastrous fracture may be attributed primarily to the arc craters, which aggravated the stress concentration resulting from the stiffening effect of the chock base and from the mechanical notch between the two welds at the end of the chock base. The metallurgical effect of the overlapping heat-affected zones of the two welds, one of which was small and shallow, was probably another contributing factor. The transition temperature of the deck stringer plate, 62° F, was lower than the average values for "fracture through" and "fracture end" plates of comparable thickness (0.99 in.) and was the lowest of all the "fracture source" plates which have been tested. Thus, the plate itself was not abnormally notch sensitive, compared to other ship plates, as measured by the V-notch Charpy test. However, in the vicinity of the welds and the arc craters the notch sensitivity was probably greatly increased, as has been observed in a number of other plates.*

Figure 18 shows the region of the starting point of a fracture, in another tanker that broke in two at sea. The origin of the fracture was at an old crack in the top of the half-round scuffing bar which was welded to the top of the sheer strake. The scuffing bar and upper sheer strake had been deformed at some previous time apparently by bumping against a mooring post or the like, and several small cracks (Fig. 19) had been formed in the top of the half-round bar, at a point between two parallel weld beads near the end of a chock base. (This end of the chock base was not perfectly aligned with the top of the sheer strake, and was welded to the half-round, slightly outboard of the weld joining the top of the half-round to the top of the sheer strake.) The heat-affected zones of the two small shallow welds overlapped, but the welds were not joined. The cracks started in the overlapping heat-affected zones, probably as a result of the restraint imposed by the chock base when the deformation occurred. The surfaces of these cracks were badly corroded, indicating that they had existed for some time without further propagation. However, when the ship encountered a critical combination of sea and loading conditions, combined with the factors of stress concentration, low air temperature and resulting increased notch sensitivity, one of these cracks propagated downward through the half-round, then through the lower weld into the sheer strake, and the catastrophic failure resulted. The steel of the half-round (NBS Plate 61A) was inherently more notch

* It appears significant that in two of the three plates of the fracture source category which had transition temperatures below 70° F, the fractures originated at arc craters. (NBS plate numbers 52A and 21A, see Fig. 11.)

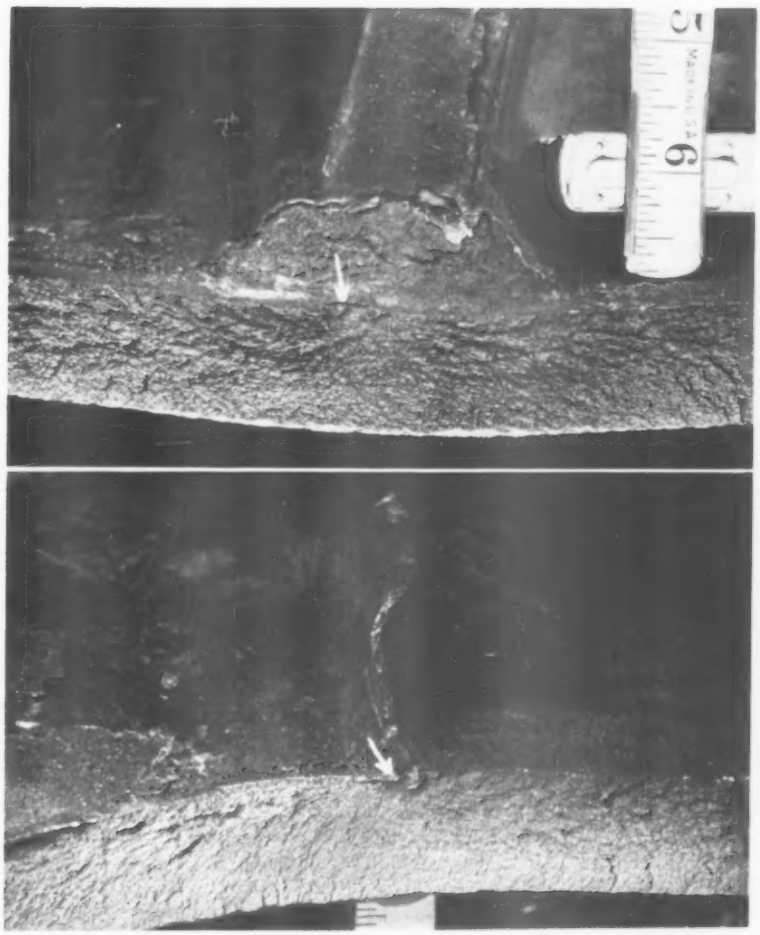

Fig. 17 The starting point of the fracture shown in Figs. 15 and 16

The fracture source (arrows) is at two small arc craters between the fillet weld to the chock base on the after section (*above*) and the toe of the light weld to the clip on the forward section (*below*). × 0.75 approximately.

Fig. 19 Top view of half round (right) and sheer strake (left) shown in Fig. 18

***C* and *D* show locations of specimens cut from sheer strake and half-round for metallographic examination and hardness tests. *E* and *F* indicate vertical longitudinal cracks in the top of the half-round under the weld, *W*, to the chock base. *S* indicates a third vertical crack, which became the source of the main fracture. Note the extent of corrosion in the top parts of the cracks, which indicates that they had existed for some time. × 1.**

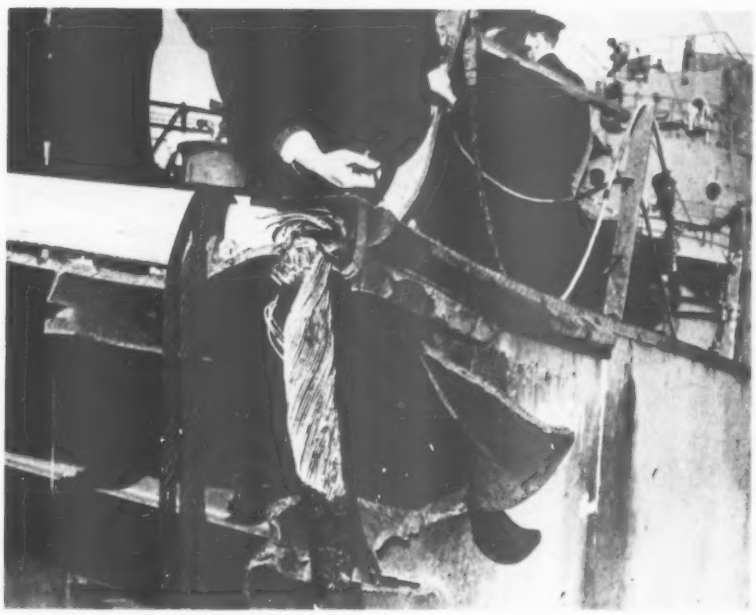

Fig. 18 After section of T1 tanker (NBS Ship No. 61) that broke in two at sea

Pencil indicates origin of fracture, at top of half-round scuffing bar welded to top of shear strake. Longitudinal tears and cuts in sheer strake and deck longitudinal resulted from battering of two parts of ship after initial fracture. (Bottom, in relatively warm water, held together for 45 min. after deck and sides had fractured.) (U. S. Coast Guard Photo.)

sensitive than the average for ship plates. At the location of the fracture source, the notch sensitivity was further increased as a result of the small shallow welds and of the previous deformation, or strain aging subsequent to the deformation. The high phosphorus and nitrogen content of this steel suggests that it might be a Bessemer steel, a material which is usually found to be notch sensitive and which is subject to excessive strain aging.[16] This material is not acceptable for use as a strength member in hull structures.[7, 8, *]

An example of a shear fracture in 0.62-in. plate from the side shell of a tanker is shown in Fig. 20. The fracture, propagating from below, was brittle near the bottom of the sample (not shown in the photograph), and changed to the shear type about 13 in. below the longitudinal,

* Both the present[8] and the preceding[7] ABS specifications require that the steel used in the construction of the hulls of vessels shall be made by either or both the open-hearth or the electrical furnace process. (Ref. 8, section 39, paragraph 8.) The present specification also specifies a limit of 0.04 % maximum for the phosphorus content.

Fig. 20 Example of a shear type fracture in 0.62-in. plate (NBS Plate No. 56B)

The shear fracture, propagating from below, stopped in the plate a few inches above the longitudinal. A brittle fracture, propagating from above and forward, ended when it ran into the shear fracture, indicating that the shear fracture had occurred first and that the brittle fractured occurred under increased stress as the ship broke in two. × 1/4 approximately.

near the center of the plate width. The shear fracture ran upward and forward, with several sharp changes in direction, passed the end of a longitudinal (at a bulkhead interruption) and ended in the shell plate about 4 in. above the longitudinal. A second brittle fracture propagating from above and forward, ended when it ran into the shear fracture about 2 1/2 in. above the longitudinal. This indicates that the lower (shear) fracture had occurred first at this point, and that the brittle fracture above occurred later as a result of increased stress as the ship broke in two. Accurate determinations of the reduction of thickness could not be made, as the sample was badly corroded and pitted. However, measurements made after cleaning the fracture edges and the nearby plate surfaces indicated that near the brittle fractures the reduction of thickness was of the order of 1 to 4%, the reduction appearing to increase slightly near the point where the fracture changed from brittle to shear type. In the two small areas of double shear fracture, near the bracket, the thickness reduction was about 30%, or more than 10 times the reduction usually observed at brittle type fractures.

Charpy V-notch specimens taken near the shear fracture, near the lower brittle fracture, and at some distance from the fractures showed that the notch sensitivity of the steel was not affected appreciably in areas as near as 1 1/2 in. to the brittle fracture. However, the notch sensitivity was increased considerably in the areas near the shear fracture, the effect being noticeable in specimens as far as 8 in. from the fracture. The 15 ft-lb transition temperature of the plate (omitting specimens near the shear fracture) was —2° F, the lowest found for any fractured ship plate tested to date. For similar standard specimens taken with their centers 2 to 5 in. from the shear fracture, the average transition temperature was 56° F, the increase probably being due to the deformation and subsequent strain aging.

Part II. Laboratory Investigations of Plates Removed from Fractured Ships

METALLOGRAPHIC EXAMINATIONS OF THE STEELS

Specimens for metallographic examination were taken from nearly all of the plates that contained fracture sources, and from a number of the other plates. Generally several specimens were selected from various locations along the path of the fracture, and sections perpendicular to the fracture were examined. Metallographic features such as grain size, inclusions, banding and the like were noted, and any unusual features were recorded and photographed.

The microstructures were typical of those of commercial quality hot-rolled carbon-steel plates. A few steels contained large numbers of nonmetallic inclusions, while a few others were excessively banded, but in general the microstructures were satisfactory for this type of material. Several of the plates were of rimmed steel as indicated by the carbon-free rims at the plate surfaces. Most of the steels, however, were semi-killed.

A large number of the specimens were examined by many different metallographic means to determine the extent of the deformation in areas adjacent to the fractures. These means included strain etching with several different reagents. The specimens were examined under polarized light, darkfield, oblique and conical illumination. Furthermore, microhardness determinations were made on many samples.

In the vicinity of the brittle fractures, the areas of deformation were extremely narrow and were in fact usually confined to the grains in which fracture occurred. In cases of ductile fracture the deformation extended for some distance on each side and ahead of the fractured grains. This indicates that the deformation was highly localized in the case of brittle fracture, and that in the case of ductile fracture deformation occurred over a much larger volume of the metal.

Microhardness measurements made with the elongated Knoop indenter revealed pronounced directional variations of hardness within grains in the vicinity of

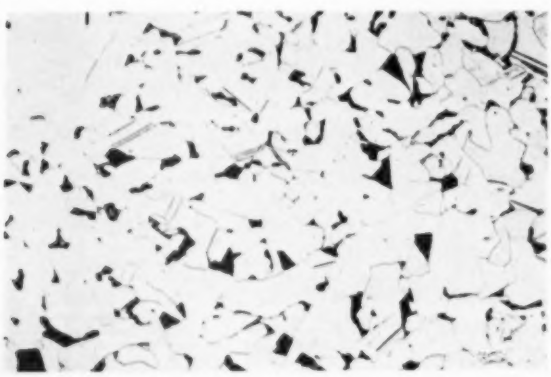

Fig. 21 Neumann bands 1/4 in. from fracture. × 100

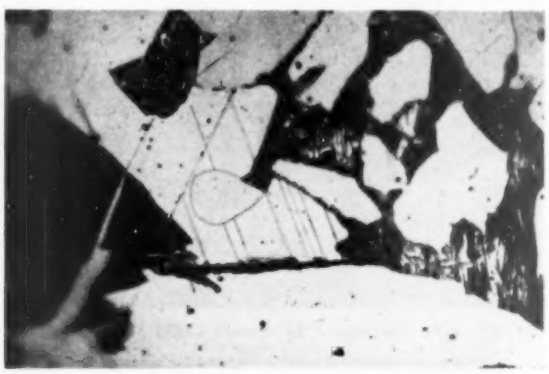

Fig. 22 Neumann bands and cracks. × 500

brittle fractures and extending for some distance from the fractures. These differences appeared to be related to the grain orientation rather than to the stress conditions or the extent of deformation. Indentations made at right angles in a single grain, or parallel indentations in adjacent grains of different orientations, usually indicated hardness differences as great as the differences between fractured grains and grains remote from the fracture.

Two unusual features were found in many of the specimens. These were Neumann bands or deformation twins, shown in Figs. 21 and 22, and directional cracks, shown in Figs. 22 to 26. Usually both features were present in a specimen adjacent to a brittle fracture, infrequently only one was found, and in a few cases neither was observed in the areas examined. Neither Neumann bands nor directional cracks were found, in any case, associated with fractures of the ductile or shear type. They were found only near the brittle fractures in the ship plates, generally not more than 1/4 in. away, but occasionally as far as 1 in. Neumann bands were found only in the ferrite and not in the pearlite.

The numerous "secondary" directional cracks that were found adjacent to the brittle fractures are believed to have been formed at the same time and in the same manner as the primary fracture. They were transcrystalline, straight and in the same direction within a single grain, but changed directions when going from one grain to another.

Occasionally the cracks are so numerous and so close together as to form a zone of shatter (Fig. 24). When this occurs, the metal between the cracks is very much distorted, showing distinct evidence of cold working. Another interesting phenomenon is the apparent displacement or slip of metal along the cracks, indicating the adjustment of stresses in the immediate vicinity of the crack. Good evidence of this is shown in Figs. 25 and 26.

Neumann bands, or deformation twins, may be formed in ferrite at room temperature by impact or shock or at low temperatures by slow deformation.[17] There is no definite agreement at present in the several theories regarding the method of formation or the reason for the formation of deformation twins. However, it has been found that twins are formed more readily under conditions similar to those that may increase the tendency to brittle fracture, such as low temperatures and high rates of strain.

DETERMINATION OF FRACTURE GRAIN SIZE

At a later stage in the investigation, a knowledge of the grain sizes of all of the plates was required, for determination of the relation between grain size and notch sensitivity. Because of the large number of plates involved, it was felt that a rapid, reasonably accurate method of determining grain size was desirable. The method adopted requires no specimen preparation, and is very similar to the method of Shepherd for tool steels, as described in the *ASM Metals Handbook*, 1948 edition, page 405.[18] The grain size thus determined has been called "Fracture Grain Size" and for most practical purposes is the same as the Austenitic Grain Size, as shown in Fig. 15 of the reference.

Charpy V-notch specimens that had been broken at temperatures low enough to obtain almost completely brittle fractures were selected from each plate. The fractures of these specimens were compared visually with the fractures of a set of similar specimens having known (microscopically determined) austenitic grain size Nos. 2 through 7. Independent comparisons were made by at least two observers on 3 Charpy specimens from each plate, and the average value obtained was

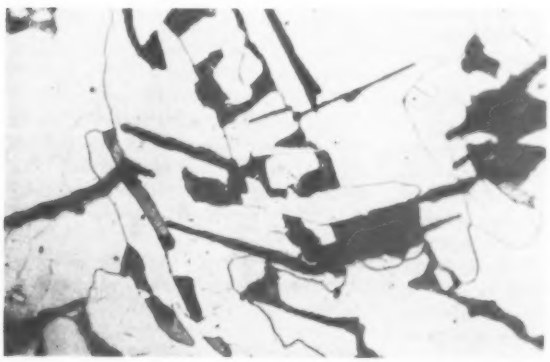

Fig. 23 Directional cracks adjacent to main fracture. × 500

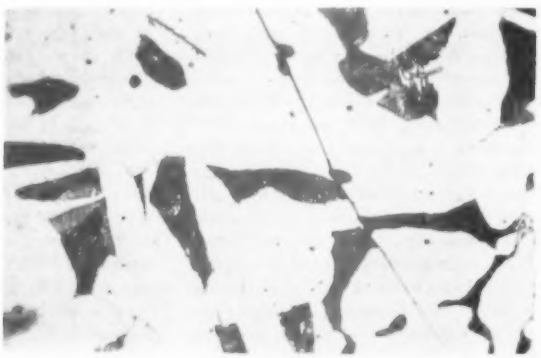

Fig. 25 Displacement of fractured pearlite grains along crack. × 500

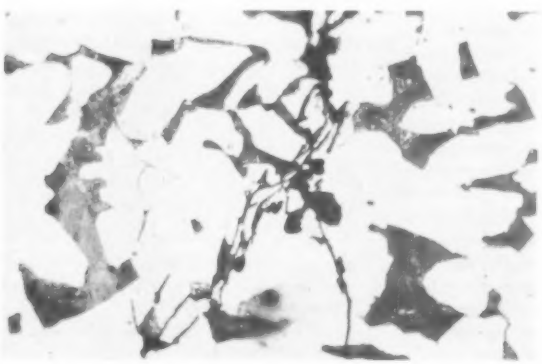

Fig. 24 Shattered zone near main fracture. × 500

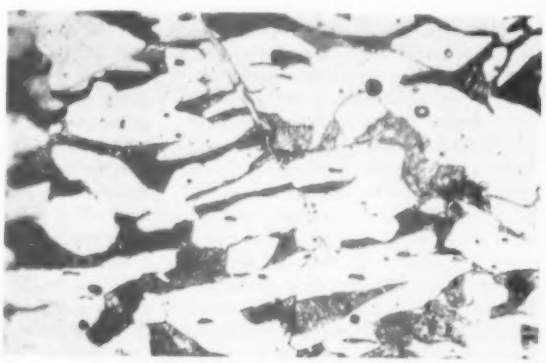

Fig. 26 Displacement of fractured grains along crack. × 500

taken as the fracture grain size number of the plate. A number of determinations made by this method were compared with microscopic determinations. In nearly all cases, the determinations checked within one-half ASTM grain size number, and none was off by more than one ASTM number.

MECHANICAL PROPERTIES OF THE STEELS

Selection and Preparation of Test Specimens

Specimens for the determinations of the mechanical properties of the plates (chiefly notched-bar and tension tests) were selected, in so far as possible, to represent the properties of the plates as fabricated in the ship. Test specimens were taken from areas that had been bent during fabrication, but areas that had been deformed as a result of the casualty were avoided. The location of each specimen and of any visible features that might affect the properties were recorded on a sketch drawn to scale. The test results for each specimen were examined and compared, and specimens that had obviously been affected as a result of proximity to welds or flame cuts, or of damage incident to the casualty, were not used for determination of the properties of the plate. These specimens were used, however, for determination of the effects of fabricating operations and, on some of the plates, additional specimens were taken to determine the effects of welds, flame cuts or deformation. As it was realized early in the investigation that the notched-bar tests were more significant than the tension tests, priority was given to the notched-bar specimens in those cases where the amount of material was limited.

For the notched-bar specimens, blanks 4 to 10 in. long were sawed out and milled or shaped to a width of 2.165 in. (equal to the length of a Charpy specimen). The individual specimens were then laid out, marked on the ends for identification of the plate and for the orientation of the notch, and numbered consecutively, before sawing them from the blank. Each specimen was shaped, or milled with a fly-cutter, to approximate size, and wet surface-ground, on all four sides, to the final dimensions. The V-notches were cut with a formed milling cutter, using one or more roughing cuts and a final finishing cut of 0.002 to 0.003 in. Before notching a batch of specimens, the sharpness and contour of the cutter were checked by notching a sample specimen and comparing the image of the notch at 50 × magnification with a scaled template on the ground-glass of a metallurgical microscope. The shape and depth of the notches were checked frequently in the shop with an optical projector and special micrometers, and samples from each batch were again checked in the laboratory.

As steels are generally not homogeneous throughout the plate thickness and rolled plates are nonisotropic due to the effect of the direction of rolling, the location and orientation of the test specimens are important factors in the determination of the notched-bar properties. The depth of the rim in rimmed steel, the amount of surface material removed, and the relation of the notch to the surface and core material are variable factors which may affect the values obtained for energy absorption, especially in specimens notched parallel to the plate surface. Also, in plates containing stringers, segregation bands or laminations, the plane of these discontinuities would be perpendicular to the direction of propagation of the fracture in a specimen notched parallel to the plate surface. This might lead to spuriously high values of energy absorption, because a large stringer or lamination may act as a miniature crack arrestor. As the discontinuities are elongated in the rolling direction and are usually small in the transverse dimension, this effect is greater in longitudinal specimens, and may or may not occur in transverse specimens.

In a transverse specimen notched perpendicular to the plate surface, stringers or voids, elongated parallel to the direction of rolling, would also be parallel to the plane of fracture in the test specimen, and would generally tend to decrease the energy absorption. The same may occur in a transverse specimen notched parallel to the plate surface if the stringers or voids are very narrow in the transverse dimension.

In longitudinal specimens notched perpendicular to the plate surface, the effect of the variables mentioned above is much less, as the stringers are perpendicular to the plane of the fracture, and laminations or segregation bands are in a plane parallel to the direction of propagation of the fracture and perpendicular to the fracture surface. Notched-bar specimens from a number of the ship plates were tested in the different permutations of specimen orientation (longitudinal or transverse), notch orientation ($\parallel$ or $\perp$ to plate surface and location of the specimen and the notch with respect to the plate surfaces. It was found that the energy absorption at a given temperature was more uniform and reproducible for the $L\perp$ (longitudinal, notch perpendicular to plate surface) specimens. This orientation was chosen as the standard for these tests for that reason, and also because in specimens of this orientation the direction of propagation of the fracture is similar to that of the majority of the fractures which occurred in service. In the few instances where the fracture was parallel to the direction of rolling of the plate, where the rolling direction was transverse to the length of the ship or where the rolling direction was uncertain (as in small insert plates), additional specimens in the transverse direction were tested for comparison.

The standard specimens used in the ship-plate tests were taken as near to the plate surface as possible, so as to include both rim and core material in the test sample. In machining, only enough material was removed from the plate-surface side to clean up the specimen. The tests of specimens taken from the center of the plate thickness appear to indicate that in a rimmed steel, the interior of the plate is considerably more notch sensitive than the areas near the plate surfaces, probably due to the lower carbon content of the rim material. In semikilled steels, the difference is less and may even be in the opposite direction.

*Tensile Properties of the Steels**

The specifications for steel for hull construction in effect at the time of purchase of these steels[7] were predicated on a heat-sampling basis and required only a cold-bend test† and a tensile test, and that the steels be made by the open-hearth or electric furnace process. In 1948, the specification requirements were modified[8] to include certain chemical requirements for steels of more than $^1/_2$ in. thick, and some changes were made in the requirements for tension test properties.

Tension tests were made on a large proportion of the plates, but because of the limitations of the material it was necessary to use several different types and sizes of tension-test specimens. A few of the plates were tested in duplicate with specimens of different types.

The tension-test results indicated that the plates that fractured in service would probably have passed the acceptance tests required at the time of purchase. The properties determined from the tension tests of the fractured plates indicate no relation that is sufficiently marked to differentiate between the source, through or end plates.

Charpy V-Notch Test Data

To establish the Charpy V-notch test curve for each plate, specimens were tested at various temperatures in the transition range. Usually 4 specimens were tested at each temperature, and the average value was plotted. To eliminate as far as possible the effect of local variations in the plates, the 4 specimens were selected from different areas of the plate according to the following scheme: The consecutively numbered specimens were divided into 4 consecutive groups, for for example 1–8, 9–16, 17–24, and 25–32.

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* See Appendices 1 and 2.

† The cold-bend tests were not generally made on the failed plates in this investigation because in most cases the samples received were badly corroded and a fair test could not be made on the required full-plate thickness specimens. Frequently the limited amount of material available could be used to better advantage for other tests.

The first specimen in each group was designated as Set A (1, 9, 17 and 25), the second as Set B (2, 10, 18 and 26), etc. The sets were then selected at random for the different test temperatures, but if consecutive sets were drawn for tests at adjacent temperature points, a second selection was made.

Occasionally several consecutively numbered specimens showed abnormally high or low values of energy absorption, compared to the other specimens tested at the same temperature. If a reason for this abnormal behavior could be found, such as proximity to a flame cut or a weld, evidence of local deformation or straining incident to the casualty, weld beads revealed on etching the broken specimens or gross laminations, these specimens were disregarded in computing the average value.

The upper part of Fig. 27 shows the Charpy V-notch test curves for some plates in which fractures in the ships originated, and the lower part shows curves for plates of corresponding thickness, in which fractures ended. The points representing the average values of energy absorbed in the multiple tests of each plate at each selected temperature are connected by straight lines, with no attempt to smooth the curves. The letter *T* on some of the curves indicates the temperature, if known, at the time of the ship failure. To compare the plates at the same energy levels, the temperature at which the curve crosses the line of 15 ft-lb energy absorption is taken as the 15 ft-lb transition temperature of the plate. The horizontal bars superimposed on each set of curves represent the range of 15 ft-lb transition temperatures of the plates in each category, and the vertical bars represent the range of average values of energy absorbed in tests at 30 and at 70° F.

It may be seen that the transition temperatures of the fracture source plates were higher, and the values of energy absorption at corresponding temperatures were lower than for the plates in the fracture-end category. In orther words, the plates in which fractures originated were considerably more notch sensitive than the plates in which the fractures ended.

This difference was observed early in the investigation, after only a few plates had been tested. In the absence of definite knowledge of the stresses and stress concentrations that existed in the plates at the time of the fractures, it appeared that some information might be gained by comparing the notch sensitivity and other properties determined in the laboratory to the nature of the fractures that occurred in service. Therefore the fractured plates were classified into three categories: Those which contained the source of a fracture (in the ship failure), those which were fractured through and those which contained the end of a fracture, as defined previously.

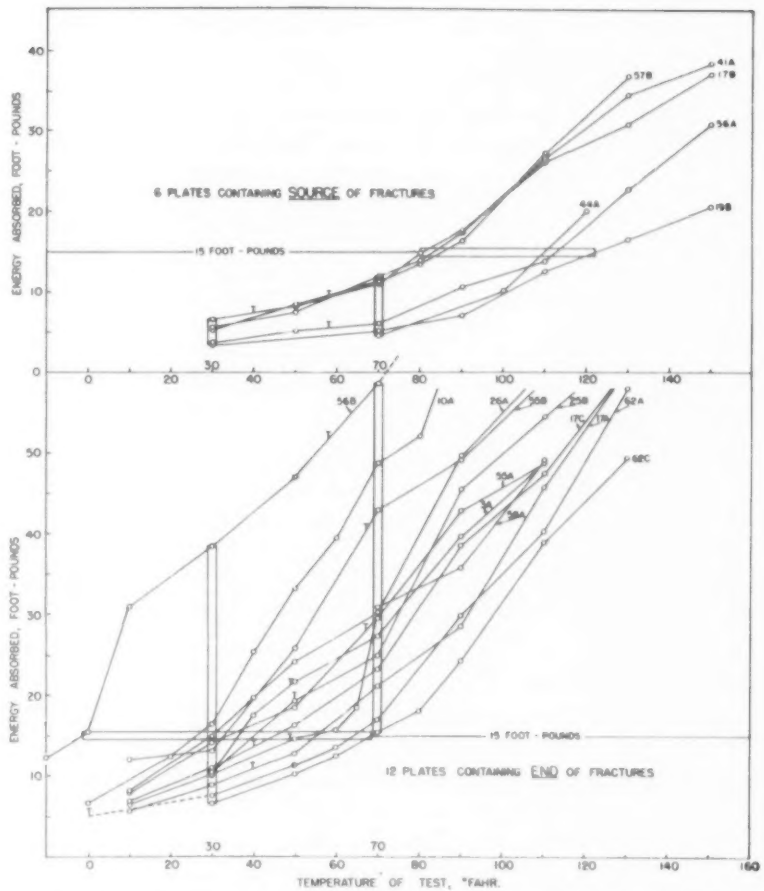

Fig. 27 Charpy V-notch test curves for some fractured ship plates 0.44–0.69 in. in thickness

Horizontal bars indicate range of 15 ft-lb transition temperatures for plates in which ship fractures originated (*above*) and plates in which fractures ended (*below*). Vertical bars indicate range of energy absorbed in tests at 30 and 70° F. Temperatures at the time of the ship failures are indicated by the letter *T* on the curves.

In this report the plates are divided into three thickness groups, selected arbitrarily so that approximately equal numbers of plates would be included in each thickness group. Group A includes plates less than 0.70 in. thick, the lower limit being determined by the smallest plate thickness from which a standard Charpy specimen could be obtained. Group B includes plates of thickness 0.70–0.80 in., inclusive (nominal $^3/_4$-in. plate), providing a group in which variations due to plate thickness are minimized; and plates more than 0.81 in. thick (up to 1.50 in., the maximum thickness plate received) are included in Group C. For the analysis of the relationship between the nature of the fractures that occurred in service and the notched-bar properties of the plates involved, all plates that were not strictly "hull plates from welded ships in which fractures occurred in normal service" were placed in a separate miscellaneous group, D. This group includes plates in which the fractures resulted from explosion or collision damage, plates from riveted ships, fractured plates that were not a part of the hull structure (rudder tube, scuffing bar, crack arrestor strap and masts), plates that contained no fracture although fractures occurred in adjacent plates or welds, a few plates in which the category of the fracture was doubtful, and some of the Ship Structure Committee "Project Steels."

A summary of the notched-bar test data, location and nature of the fracture source and related information for each plate in the fracture source category is given in Table 2. A condensed summary of the numerical data for all plates in the source, through and end categories, and for each thickness group, is presented in Table 3

Figures 28 and 29 show graphical comparisons of the notched-bar properties of the plates. In both figures, the horizontal bars represent the range of observed values for the plates in each category and thickness group, and the vertical lines in the

Table 2—Summary of Data on Fracture Source Plates for Which Transition Temperatures Were Determined

NRS Plate No.	Plate Thickness Inches	Temp. at Failure °F	Energy at Failure Temp. Ft.-lb.	15 ft.lb Trans. Temp. °F	Type of Vessel, Location and Nature of Fracture Source, Remarks
			A. 6 Hull Plates, Thickness 0.44"-0.69"		
19B	0.69	-A	-	122	Z-ET1-S-C3. Insert plate, fwd. corner No. 3 hatch. Source at hatch corner, in flame cut corner of opening.
56A	0.65	58W	5.5	113	T2-SE-A1. Shell, G strake stbd. Secondary source near weld at angle of fluted trans. bhd. 52. Original source probably in bottom. Ship broke in two.
44A	0.52	-A	-	110	EC2-S-C1. Shell F13 port, fr. 152. Source reported near longitudinal seam weld, end at rivet hole in same plate. Welded previous night. Temperature changed 25°F.
57B	0.61	-A	-	85	Mod. C2. Channel, top of above-deck long'l. girder. Source near hatch corner at notch between welds to platform deck plate and to hatch side doubler plate.
41A	0.69	58A	9.3	83	EC2-S-C1. Sheer strake J9 port, at flame cut top edge near end of freeing port in bulwark. Fracture end in same plate, near lower seam weld.
17B	0.63	40W	7.4	82	EC2-S-C1. Curved bilge plate D8 port. Source reported near weld to tank top. Cracked in drydock as dock was drying. Air temp. 75°F, water 40°F.
			B. 12 Hull Plates, Thickness 0.70"-0.80"		
20A	0.71	37A	3.2*	153	EC2-S-C1. Deck stringer, stbd., fr. 86 at aft edge of welded doubler, fwd. corner of deck house. Highest transition temperature of all hull plates tested.
18A	0.71	50A	4.2*	151	Z-ET1-S-C3. Deck plate, fr. 113 at after corner of deck house. Source near welds to deck house (above) and to transverse bhd. (below).
34A	0.71	51A	3.2*	140	EC2-S-C1. Deck insert plate. Source at fwd. corner No. 3 hatch. Rolling direction transverse to length of ship. Trans. temp. 180°F for transverse specimens.
72A	0.72	55A	6.6	112	Z-ET1-S-C3. Deck stringer plate. Source near intersecting and overlapping welds at edge of doubler pad.
75B	0.75	42W	5.0	102	T2-SE-A1. Shell, D10 port. Source at end of interrupted longitudinal, near transverse bulkhead.
76A	0.75	62W ?	8.2 ?	98	T2-SE-A1. Shell F12 port, curved bilge plate. Source at end of bilge keel.
9B	0.72	-A	-	91	EC2-S-C1. Sheer strake J9 stbd., at ladder cutout fr. 90. Crack about 1" long, origin at rough flame-cut edge near change of section.
9A	0.71	-A	-	83	EC2-S-C1. Sheer strake J9 port, at ladder cutout, fr. 90. Same as 9B above. See Figure 7.
8A	0.75	42W	7.4	79	T2-SE-A1. Shell, D7 port, curved bilge plate. Source at end of interrupted longitudinal near bhd.62, 14" below DE seam. Ship broke in two.See Figure 12.
75A	0.75	42W	7.0	75	T2-SE-A1. Shell, E15 port. Secondary source at end of interrupted longitudinal, near transverse bulkhead.
21A	0.72	45A	11.4	69	Z-ET1-S-C3. Sheer strake J11. Source at change of section and arc crater on flame cut edge. Steel very hard near weld and flame cut. See Figures 9 and 10.
75C	0.71	42W	10.8	63	T2-SE-A1. Shell C11 port. Secondary source at end of long'l. Short crack at undercut weld, side of tripping bracket. Plate not fractured completely thru in some areas.
			C. 10 Hull Plates, Thickness 0.81"-1.25"		
32A	1.25	55A	6.0	128	C3-S-A2. Deck plate at after port corner No. 3 hatch. Source exactly at corner of flame-cut hatch opening. See Figure 6.
29B	0.83	66W	7.8	102	T2-SE-A1. Shell E16 stbd., fr. 55; curved bilge plate. Source at end of bilge keel, end in same plate. Bilge keel had recently been straightened by "application of heat and force".
46A	1.25	37A	5.5	101	T2-SE-A2. Sheer strake, port, fr. 60. Secondary source at small weld bead in sample. Reported last plate to fracture as ship broke in two. See Figure 1.
32B	1.27	55A	6.5	97	C3-S-A2. Deck plate at after stbd. corner No. 3 hatch. Source at junction of welds at hatch corner.
83A	0.87	52A	7.9	88	VC2-S-AP2. Deck stringer, stbd. Source at square corners of cutout where padeye had been welded thru deck to girder below. Padeye was cut off flush with deck and covered with light seal weld.
70A	1.19	46A	9.0	84	C3-S-A2. Insert plate, No. 3 hatch. Fracture started at after stbd. hatch corner in diagonal weld about 7" long, apparently a repair weld of a previous crack.
25A	0.82	53W	10.6	78	T2-SE-A1. Shell E14 port, fr. 56-57; curved bilge plate. Source at weld to longitudinal.
12A	0.85	42W	9.5	76	Tanker, not MC Type. Shell, D6 stbd.; curved bilge plate. Source at end of longitudinal, reported as "fatigue crack". Actual source not available for examination.
92A	0.81	55W	10.0	73	T2-SE-A1. Shell, E strake port, fr. 61-62. Source at notch between serration of bilge keel and doubler at fwd. end of b.k. Appearance of "fatigue crack" found due to heat affected zones of fillet welds to bilge keel plate.
52A	0.99	34A	8.0	62	T2-SE-A2. Deck stringer, stbd., fr. 56. Source at arc crater and notch between weld to chock base and weld of small clip to deck. See Figures 14, 15, 16.
			D. 10 Miscellaneous Plates and Shapes		
50A	1.50	20A	3.3*	161	VC2-S-AP2. Tubular main mast formed from plate, fractured 85% while testing cargo boom. Source near weld at mast house top. Transition temperature varied with location; highest near fracture source.
23A	1.00	-W	-	144	EC2-S-C1. Rudder tube, 16" O.D. Fracture source at fatigue crack starting at plug weld near shelf plate. Lower half of rudder lost.
82B	0.86	60A	4.7	143	C2-S-AV1. Deck stringer, frame 85 port. Probable source, near weld to sheer strake. Fracture welded, so exact source could not be determined.
51B	0.48	32A	4.4	135	Riveted Collier. Inboard deck plate. Probable source near hatch corner. Brittle fracture changed to shear near rivet holes. Rivet hole spacing indicates 10% elongation near shear fracture.
28A	1.00	32W	4.6	106	U.S.C.G.C. Shell, E2 port, fr. 18. Rivetted construction. Fracture source at rivet hole, end in same plate. Ice damage.
61A	1.5x3	35A	8.1	100	T1-MB-T1. Half round scuffing bar at top of sheer strake, fr. 35. Non-structural attachment, probably Bessemer steel. Source between adjoining weld beads, at old crack caused by previous deformation. See Figs. 17 and 18.
49A	1.25	27A	5.5	90	VC2-S-AP2. Foremast, lower strake. Formed from plate-possibly transverse. Source near internal lap weld to second strake.
40A	0.77	-A	-	88	EC2-S-C1. Crack arrestor strap. Source at punched rivet hole near flame cut edge. Changed to shear fracture near end of crack.
80B	0.75	58W ?	8.0 ?	81	T2-SE-A1. Shell, A8 port. Source reported near outboard edge of plate, probably near bulkhead intersection. Actual source not examined.
27A	0.44	-A	-	80	A00. Port bow, above sheer strake. Collison damage. Source near seam weld, end in same plate.

? Temperatures reported when the fracture was discovered, but reports did not indicate that the exact time of fracture was known.

* Values determined by extrapolation of notched bar test curve.

Table 3—Summary of Charpy V-Notch Test Data

Fracture Category and Thickness Group	Average Plate Thickness Inches	Temp. at Failure Average °F	Temp. at Failure Range °F	Number of Plates N	Energy at Failure Temp. Average Ft.-lb.	Energy at Failure Temp. Range Ft.-lb.	Energy at Failure Temp. Std. Dev. Ft.-lb.	15 Foot-Pound Transition Temp. Average °F	15 Foot-Pound Transition Temp. Range °F	15 Foot-Pound Transition Temp. Std. Dev. °F	Number of Plates N
Fracture SOURCE Plates											
A. Hull Plates 0.44"-0.69"	0.63	52.0	40-58	3	7.4	5.5- 9.3		99.2	82-122	16.2	6
B. Hull Plates 0.70"-0.80"	0.73	46.8	37-62	10	6.7	3.2-11.4		101.3	63-153	30.1	12
C. Hull Plates 0.81"-1.50"	1.01	49.7	34-66	10	8.0	5.5-10.6		88.9	62-128	17.9	10
All Hull Plates	0.81	48.7	34-66	23	7.35	3.2-11.4	2.24	96.4	62-153	24.3	28
D. Misc. Plates and Shapes	0.95	37.7	20-60	7	5.5	3.3- 8.1		112.8	80-161	28.5	10
ALL SOURCE Plates	0.85	46.2	20-66	30	6.92	3.2-11.4	2.26	100.7	62-161	26.5	38
Fracture THRU Plates											
A. Hull Plates 0.44"-0.69"	0.62	40.7	0-78	15	10.2	4.5-18.7		58.2	24- 85	15.7	16
B. Hull Plates 0.70"-0.80"	0.73	43.0	24-60	14	11.1	6.0-16.0		61.7	41- 89	12.4	15
C. Hull Plates 0.81"-1.50"	0.93	37.7	20-60	12	7.2	3.8- 9.9		79.8	57-102	14.4	14
All Hull Plates	0.75	40.6	0-78	41	9.61	3.8-18.7	3.38	66.1	24-102	17.0	45
D. Misc. Plates and Shapes	0.69	36.0	30-54	5	6.8	4.1- 8.2		75.8	41-112	21.2	7
ALL THRU Plates	0.75	40.1	0-78	46	9.31	3.8-18.7	3.36	67.4	24-112	18.0	52
Fracture END or NO FRACTURE											
A. Hull Plates 0.44"-0.69"	0.57	46.7	0-67	9	22.7	5.0-51.7		35.7	-2- 68 (Minus 2°F)	20.3	14
B. Hull Plates 0.70"-0.80"	0.75	49.4	32-70	9	12.0	8.1-17.2		60.8	29- 82	17.1	10
C. Hull Plates 0.81"-1.50"	0.82	45.5	37-66	6	11.7	7.1-16.0		67.4	41- 88	13.5	8
All Hull Plates	0.69	47.4	0-70	24	15.96	5.0-51.7	10.41	51.5	-2- 88	22.7	32
D. Misc. Plates and Shapes	0.56	39.5	37-42	2	6.4	6.0- 6.8		79.3	62- 96	12.4	3
Plates with NO FRACTURE	0.67	44.2	42-49	4	17.7	8.9-25.4		46.6	24- 61	14.3	5
ALL END or N.F. Plates	0.68	46.5	0-70	30	15.55	5.0-51.7	9.97	53.0	-2- 88	22.6	40

bars (including the ends) show the values for individual plates. The circles above the bars indicate the average for all plates in each group.

The comparisons in Fig. 28 are based on the energy absorbed by Charpy-V notch specimens at the failure temperature of each plate. The ranges of the failure temperatures for each group, and the proportions of the plates in each category that showed energy absorptions above 10 ft-lb at the failure temperatures, are shown in the figure. Nearly half of the plates that did not contain a fracture source, but only 10% of the fracture source plates, were capable of absorbing more than 10 ft-lb in the V-notch Charpy test at the temperatures of the failures in service. The energy absorbed at the failure temperature was more than 15 ft-lb for 33% of the plates containing the end of a fracture or no fracture and for 9% of the "fracture through" plates. None of the plates in the fracture source category showed an energy absorption as high as 15 ft-lb, the highest value being 11.4 ft-lb at a temperature of 45° F. In this plate, NBS plate No. 21A (Table 2, thickness group B), the fracture originated at an arc crater on a flame-cut edge of the plate. (See discussion of Figs. 10 and 11.)

The 15 ft-lb transition temperatures of the plates are compared in Fig. 29. In addition to the average, individual values and range of observed values for the plates in each category, the range of plus or minus twice the standard deviation of the observed transition temperatures is shown by arrows extending from the circles that indicate the average values.*, [19] The upper three sections of the figure show the transition temperatures of fractured hull plates in the source, through, and end categories for each of the three thickness groups, A, B and C of Tables 2 and 3. The fourth section shows the values for all of the above thickness groups combined. The data for the miscellaneous plates (Group D) are shown in the lower section of the figure. This section includes two additional categories: Plates in which no fractures occurred, although adjoining plates or welds were fractured in the ship failures; and some of the Ship Structure Committee "Project Steels" that were tested at NBS. The 15 ft-lb transition temperatures of Project Steels tested at Pennsylvania State College[20] are shown for comparison, in the bar at the bottom of figure. These transition temperatures were estimated from the curves on pages 55-57 of the reference,* except that for Steel E (Fig. B-8 of Reference 20) the values shown for the rim material were used (as the specimens used at NBS were taken as near as possible to the plate surface). The estimates of the 15 ft-lb transition temperatures of the D_R and E steels, from the above reference, were about 5° F higher than the values found for these steels at the National Bureau of Standards.

A relation between the plate thickness and the transition temperatures of the plates is indicated in the upper 3 sections of Fig. 29. The thinner plates (top) have

* For a set of observations resulting from measurements made under the same essential conditions and representing material produced under the same essential condition, approximately 95% of the total observations should fall within the range: average plus or minus twice the standard deviation.[19]

* Slightly different values for the 15 ft-lb transition temperatures of some of these steels were obtained from the curves of Reference 21, probably because some of the curves, in both references were extrapolated at the 15 ft-lb level.

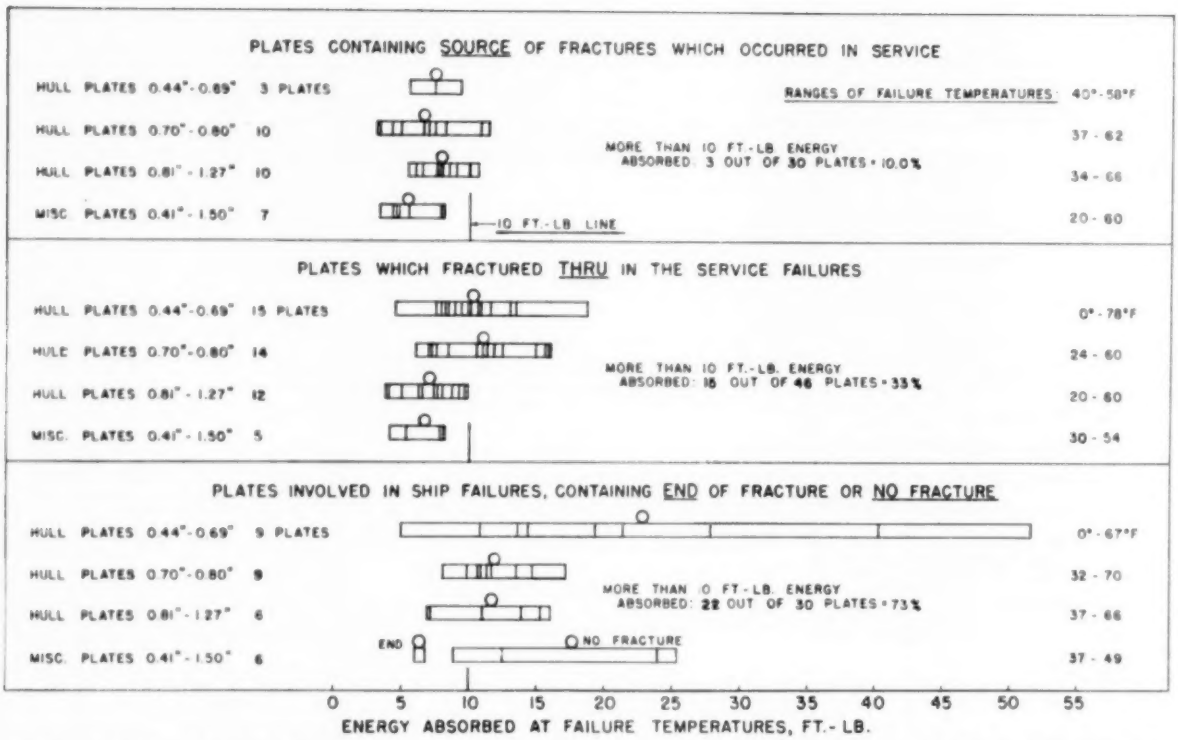
PLATES CONTAINING SOURCE OF FRACTURES WHICH OCCURRED IN SERVICE
HULL PLATES 0.44"-0.69" 3 PLATES
HULL PLATES 0.70"-0.80" 10
HULL PLATES 0.81"-1.27" 10
MISC. PLATES 0.41"-1.50" 7
10 FT.-LB. LINE
RANGES OF FAILURE TEMPERATURES:
MORE THAN 10 FT.-LB. ENERGY ABSORBED: 3 OUT OF 30 PLATES = 10.0%
40°-58°F
37-62
34-66
20-60
PLATES WHICH FRACTURED THRU IN THE SERVICE FAILURES
HULL PLATES 0.44"-0.69" 15 PLATES
HULL PLATES 0.70"-0.80" 14
HULL PLATES 0.81"-1.27" 12
MISC. PLATES 0.41"-1.50" 5
MORE THAN 10 FT.-LB. ENERGY ABSORBED: 15 OUT OF 46 PLATES = 33%
0°-78°F
24-60
20-60
30-54
PLATES INVOLVED IN SHIP FAILURES, CONTAINING END OF FRACTURE OR NO FRACTURE
HULL PLATES 0.44"-0.69" 9 PLATES
HULL PLATES 0.70"-0.80" 9
HULL PLATES 0.81"-1.27" 6
MISC. PLATES 0.41"-1.50" 6
END
NO FRACTURE
MORE THAN 10 FT.-LB. ENERGY ABSORBED: 22 OUT OF 30 PLATES = 73%
0°-67°F
32-70
37-66
37-49
0 5 10 15 20 25 30 35 40 45 50 55
ENERGY ABSORBED AT FAILURE TEMPERATURES, FT.-LB.

Fig. 28 Relation of energy absorbed by Charpy V-notch specimens at the temperatures of the ship failures to the nature of the fractures in the ship plates. Vertical lines in the bars indicate values for individual plates. Circles above bars indicate average value for each group of plates

generally lower transition temperatures, and a greater spread of values, than the thicker plates. Project Steel E also has a lower transition temperature in the 1/2-in. plate than in the 3/4 in. thickness as shown in the second bar from the bottom of Fig. 29. This indicates that the notch toughness of a steel may be improved by the lower finishing temperature or the greater amount of hot working, or other factors involved in rolling to thinner dimensions. However, the greater spread of values for the thinner plates suggests that the notch sensitivity is influenced also by other variables in the rolling process (such as reduction per pass, temperature at last pass or rate of cooling, etc.) as these variations would naturally be greater in rolling to the thinner dimensions.

In each thickness group and in the miscellaneous group the average transition temperature of the plates in the "fracture source" category is higher than the average values for plates that did not contain the source of a fracture in the ship failures. Plates in which the ship fractures ended show lower average transition temperatures than plates in the "fracture through" category, although this difference is not as great as the difference between "source" and "through" plates. Even though there is a rather large scatter within each category, and the ranges for the different categories overlap somewhat, the same consistent relationship is indicated in each thickness group by both the upper and the lower limits of the ranges of observed values of the transition temperature and (with one exception) the ranges of the scatter bands defined by the average value plus or minus twice the standard deviation.

Among the fractured hull plates (for the 3 thickness groups), the transition temperatures were below 70° F for 78% of the "fracture end" plates and for 58% of the plates that fractured through. Only 3 out of 28, or 11%, of the plates in the fracture source category had transition temperatures below 70° F, the lowest being 62° F. In two of these three plates (NBS Plate Nos. 21A and 52A, Table 2) the fractures originated at arc strikes on a flame-cut edge or near a small weld (Figs. 11 and 17). The local notch sensitivity at these points was probably considerably higher than in the unaffected portion of the plate, where the specimens for determination of the transition temperatures were taken. In the third plate (75C) there were two sources of secondary fractures: (A) an undercut weld at the side of a tripping bracket, and (B) the edge of a small globule of weld metal (another possible arc strike) at the end of a longitudinal. The crack at Source A propagated only a short distance and stopped. The crack at Source B apparently did not start until the main fracture, extending from an adjoining plate had nearly reached that point, as the two fractures joined only a few inches from Source B. On the other side of Source B, the fracture divided into two branches, one of which stopped in the plate. Several areas of this plate were not fractured completely, and on breaking open the crack in the laboratory, bright newly formed areas of shear fracture were found, near the plate surface and at a few isolated points in the interior of the plate. All of the above facts indicate considerable resistance to fracture, which is consistent with the relatively low transition temperature (63° F) of this plate.

In the miscellaneous group the trend is similar: 75% of the plates containing the end of a fracture or no fracture, 43% of the fracture through category and none of the fracture-source plates, had transition temperatures below 70° F.

STATISTICAL INTERPRETATION OF NOTCHED-BAR TEST DATA

The evidence presented above shows a very definite difference of the notch sensitivity of the plates in the different categories, whether measured by the energy absorbed at the failure temperatures of the respective plates or by the 15 ft-lb transition temperatures. A statistical evaluation of the differences shown in Table 3 between source-through and through-end plates, respectively, for these two criteria of notch sensitivity is given in Table 4. This analysis indicates that it

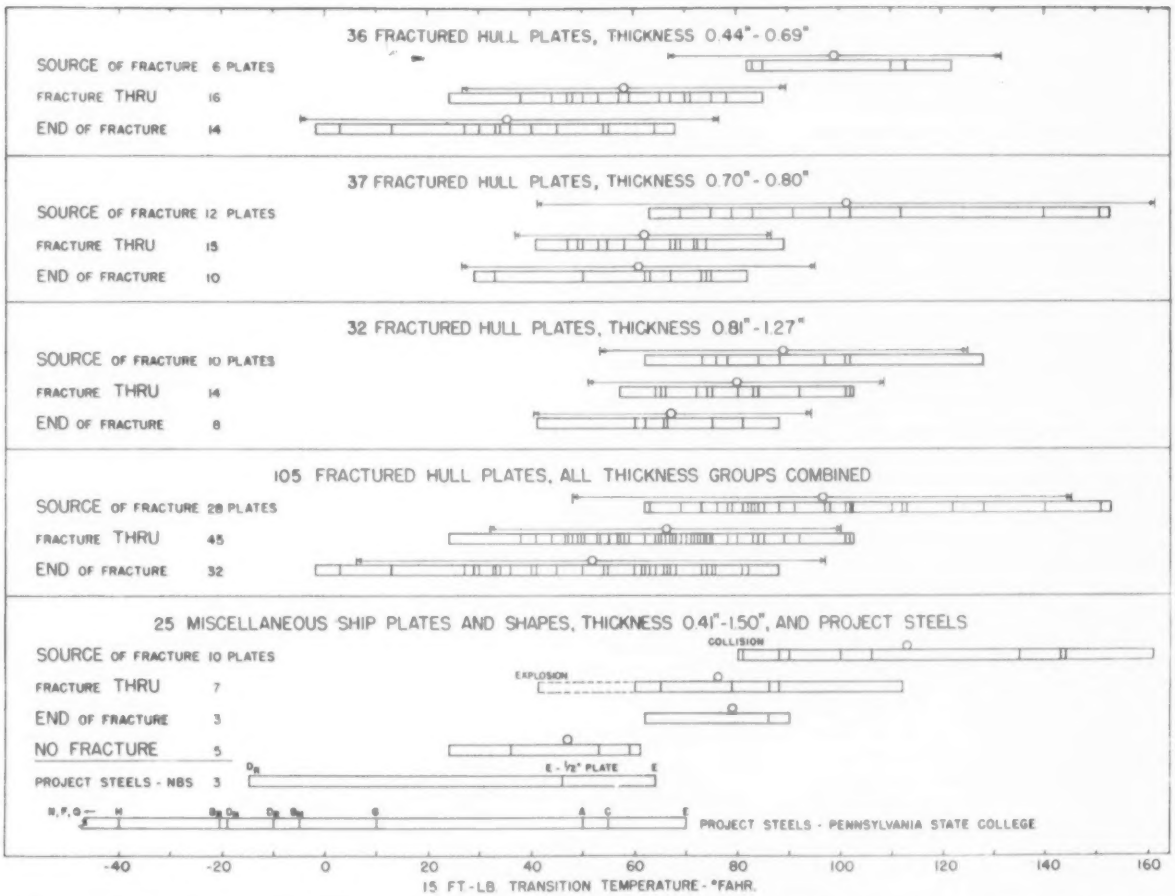
36 FRACTURED HULL PLATES, THICKNESS 0.44"- 0.69"
SOURCE OF FRACTURE 6 PLATES
FRACTURE THRU 16
END OF FRACTURE 14
37 FRACTURED HULL PLATES, THICKNESS 0.70"- 0.80"
SOURCE OF FRACTURE 12 PLATES
FRACTURE THRU 15
END OF FRACTURE 10
32 FRACTURED HULL PLATES, THICKNESS 0.81"- 1.27"
SOURCE OF FRACTURE 10 PLATES
FRACTURE THRU 14
END OF FRACTURE 8
105 FRACTURED HULL PLATES, ALL THICKNESS GROUPS COMBINED
SOURCE OF FRACTURE 28 PLATES
FRACTURE THRU 45
END OF FRACTURE 32
25 MISCELLANEOUS SHIP PLATES AND SHAPES, THICKNESS 0.41"-1.50", AND PROJECT STEELS
SOURCE OF FRACTURE 10 PLATES
FRACTURE THRU 7
END OF FRACTURE 3
NO FRACTURE 5
PROJECT STEELS - NBS 3
COLLISION
EXPLOSION
E - 1/2" PLATE
PROJECT STEELS - PENNSYLVANIA STATE COLLEGE
-40 -20 0 20 40 60 80 100 120 140 160
15 FT-LB. TRANSITION TEMPERATURE - °FAHR.

Fig. 29 Relation of 15 ft-lb transition temperature to the nature of the fractures in ship plates and comparison of transition temperatures of fractured ship plates and "project steels." The arrows represent the range of plus or minus twice the standard deviation of the transition temperatures for each group of plates

is extremely unlikely that these differences might be due to chance.

The difference of notch sensitivity of the plates in the different categories may be interpreted as follows: The fractures in the ships originated in plates that were, so to speak, "selected" because of lower than average notch toughness at critical locations in the structure. The plates in the "through" and "end" categories were "run of the mill" plates that happened to be in the path of the fracture as it propagated. In some cases, higher than average notch toughness of the plates (that is, greater ability to absorb energy) contributed to the halting of the fractures. However, there were many other contributing factors such as differences of stress concentration, dissipation of elastic energy, changes in stress levels due to transient loads, readjustment of stress distribution or the effect of nearby structural reinforcements. These factors undoubtedly account, in part, for the overlap and scatter of the notch sensitivity properties of the plates in the different categories. For example, a plate in the "fracture through" category, having a transition temperature in the range approximately 60 to 90° F (Figs. 29 and 30), might have been a fracture-source plate under severe stress conditions in the presence of a notch; or under less severe stress conditions it might have been a fracture-end plate. Similarly, many of the plates in the "fracture end" category might have been "fracture through" plates if the stress conditions at the head of the advancing fracture (in the ship) had

Table 4—Statistical Evaluation of Differences of Notched-Bar Properties of Source Through and End Plates

Quantity Compared		Energy at Failure Temp.		15 ft.-lb. Transition Temp.	
		Source-Thru	Thru-End	Source-Thru	Thru-End
Differences of Average Values $d = \bar{X}_1 - \bar{X}_2$	Hull Plates	2.26 ft.-lb.	6.35 ft.-lb.	10.3°F	16.6°F
	All Plates	2.39 ft.-lb.	6.28 ft.-lb.	13.1°F	16.4°F
Standard Error of Differences $\sigma_d = \sqrt{\frac{\sigma_1^2}{N_1} + \frac{\sigma_2^2}{N_2}}$	Hull Plates	.706 ft.-lb.	2.190 ft.-lb.	5.26°F	4.70°F
	All Plates	.643 ft.-lb.	1.886 ft.-lb.	4.97°F	4.35°F
Ratio: Difference/Std. Error d/σ_d	Hull Plates	3.21	2.90	5.77	3.67
	All Plates	3.72	3.34	6.70	3.91
Odds Against Chance Occurrence (from Probability Tables)	Hull Plates	> 726 to 1	267 to 1	> 1.7×10^{7} to 1	> 169 to 1
	All Plates	> 4637 to 1	> 1011 to 1	> 5.0×10^{8} to 1	> 1011 to 1

$\bar{X}$ = Average Value, = $\frac{\Sigma X}{N}$, where N = number of plates and ΣX = sum of individual values.

σ = Standard deviation = $\sqrt{\frac{\Sigma(\bar{X}-X)^2}{N}}$

not been mitigated by factors such as those listed above. Some plates classified in the "fracture end" category actually did fracture through under more severe stress conditions, as illustrated in Fig. 20 and discussed in the definition of the "fracture end" category.

Local variations of notch sensitivity in the plates is another probable cause of the scatter. For example, many of the fractures originated in areas that had been deformed in fabrication or in service, or near welds, arc strikes or flame-cut edges. Notched bar and other tests show that the notch sensitivity is generally greater in such areas than in unaffected portions of the plates.

It appears reasonable to believe that plates of the quality used in ship construction would show a distribution of notch sensitivity similar to the familiar Gaussian probability curve, or normal curve. Thus a small sample of plates *taken at random* would probably include only plates with "normal" values, and the probability is small that the sample would include plates with abnormally high or low values. This is shown by the project steels and by the ship plates which did not contain fracture sources; these relatively small random samples did not include any steels with transition temperatures as high as those found for some of the "fracture source" plates, although, if a much larger sample were taken it is probable that such steels would be found. The "fracture source" plates, however, were not taken at random, but were "selected" by the fact that they contained the origin of a fracture. This selection was from a much larger sample (all of the plates in service at numerous critical locations in some 5000 ships), and consequently the chance of finding plates with the abnormally high transition temperatures was much greater. Similar evidence of selection is indicated in Fig. 57 of Reference 1, which shows that ship plates fractured in service (not all of which were fracture-source plates) were generally more notch sensitive than either plates selected at random from shipyards or samples obtained from steel mills.

In Fig. 30, the above interpretation is tested by an analysis of the frequency distributions of the observed 15 ft-lb transition temperatures of all plates in the source, through and end categories. The vertical bars indicate the percent of the total number of plates in each category* (left-hand scale) or the number of plates (right-hand scale) in each 10° F interval of the transition temperatures. (For convenience the intervals are taken as 10–19, 20-29, etc., and the bars are placed at the midpoints of these intervals.) Fractured hull plates are

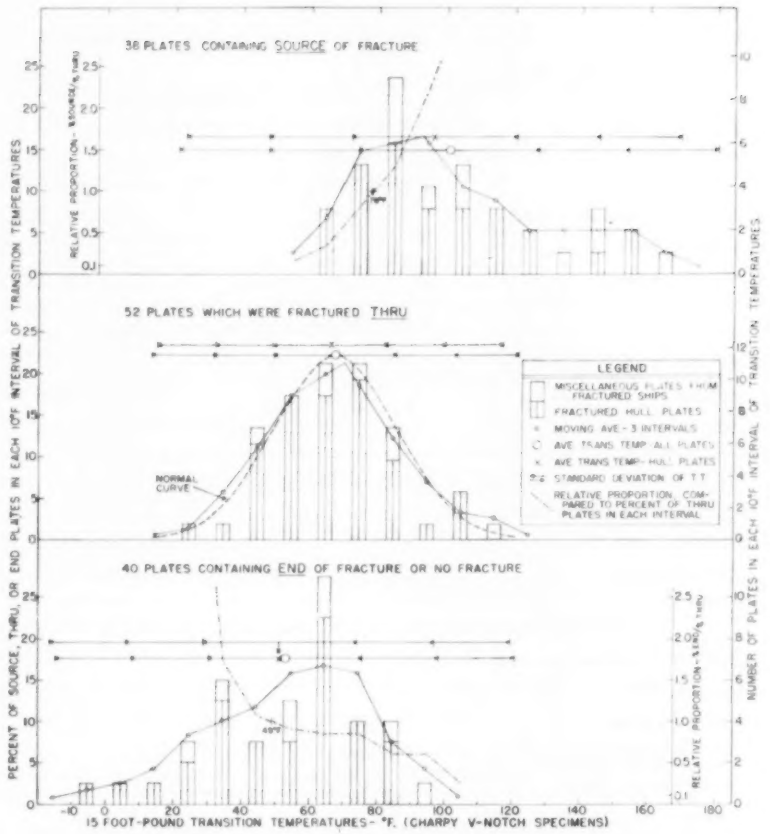
38 PLATES CONTAINING SOURCE OF FRACTURE
52 PLATES WHICH WERE FRACTURED THRU
40 PLATES CONTAINING END OF FRACTURE OR NO FRACTURE
LEGEND
MISCELLANEOUS PLATES FROM FRACTURED SHIPS
FRACTURED HULL PLATES
MOVING AVE - 3 INTERVALS
AVE. TRANS. TEMP. ALL PLATES
AVE. TRANS. TEMP. HULL PLATES
STANDARD DEVIATION OF T.T.
RELATIVE PROPORTION, COMPARED TO PERCENT OF THRU PLATES IN EACH INTERVAL
NORMAL CURVE
PERCENT OF SOURCE, THRU, OR END PLATES IN EACH 10°F INTERVAL OF TRANSITION TEMPERATURES
NUMBER OF PLATES IN EACH 10°F INTERVAL OF TRANSITION TEMPERATURES
RELATIVE PROPORTION - SOURCE/THRU
RELATIVE PROPORTION - END/THRU
15 FOOT-POUND TRANSITION TEMPERATURES - °F. (CHARPY V-NOTCH SPECIMENS)

Fig. 30 Frequency distributions of 15 ft-lb transition temperatures of plates in the fracture-source, fracture-through and fracture-end categories

The distribution of the fracture-through plates (*center*) approximates the normal curve. The fracture source plates (*top*) have a higher average transition temperature, and the relative proportion of fracture-source to fracture-through plates in each interval (dash-dot line) increases with increasing transition temperature.

represented by the center-lined portion of the bars, and the open parts of the bars represent the additional plates of the miscellaneous group. Reasonably smooth frequency distribution curves (solid lines) are obtained by plotting the moving averages (for all plates) of 3 successive intervals. The average transition temperature for all plates in each category is indicated by the symbol "O," and the symbol "X" indicates the average value for the hull plates alone. The symbols are placed at the location of the peak of the computed normal curve. Arrows extending from these symbols show the range of plus or minus one, two or three times the standard deviation. It may be seen that the average values and the general shape and position of the distribution curves are not changed significantly by the additional miscellaneous plates.

The distribution curve for the fracture-through category (Fig. 30, center) is nearly symmetrical, and closely approximates the normal curve (dashed line) computed from the values of the average and the standard deviation. The curve for the fracture-source plates (top) is decidedly unsymmetrical, and the average transition temperature is 33° F higher than the average for the fracture-through plates. The distribution curve for the plates containing the end of a fracture or no fracture is also somewhat unsymmetrical, and the average transition temperature is 14° F lower than that of the plates in the fracture-through category. A number of the plates in the fracture-source and fracture-end categories had transition temperatures respectively higher or lower than any found in the sample represented by the fracture-through plates. However, if the normal curve represented, say 10,000 plates, the ends of the normal curve, which approach zero asymptotically, would include a number of plates with higher or lower transition temperatures than were found in the relatively small sample.

The frequency distribution curve for the fracture-source plates shows that a large proportion had transition temperatures in the same range as the fracture-through plates, which might appear to indicate that there is no sharp demarcation between the transition temperatures of the fracture-through plates and those which

* By plotting in terms of percent, rather than number of plates, the curves for the three categories are reduced to the same scale so they may be compared directly, although the number of plates in each category is different.

contained a fracture source. This may be attributed in part to stress differences and local variations of notch sensitivity, as discussed previously. However, it is necessary to consider also the relative numbers of plates in each interval of transition temperatures, in the general population from which both the fracture-source and the fracture-through plates were selected. The frequency distribution of the general population cannot be derived directly from the available data, as the relative number of "source," "through," "end" and "no fracture" plates involved in the actual service failures is unknown, and bears no relation to the number of sample plates in each category.

Nevertheless, an indirect comparison may be made, for different intervals of transition temperatures, of the relative proportion of the total number of plates in each category. For example the moving average values for 3 successive intervals (from which the smoothed frequency distribution curves of Fig. 30 were plotted) indicate that plates with transition temperatures in the interval 70–79° F would be expected to include 14.9% of the total number of fracture-source plates, and 18.6% of the fracture-through plates. The ratio: % of fracture source plates/% of fracture-through plates (presumably selected from the same general population) is therefore 14.9/18.6 or 0.8 for this interval. Similarly, 15.8% of the fracture-source plates, and only 7.1% of the fracture-through plates, or a ratio of 2.2, would be selected from the 90–99° F interval of transition temperatures. In other words, plates with transition temperatures 20° F higher would furnish a slightly larger percentage of the total number of fracture-source plates, but less than half as many fracture-through plates.

The dot-dash line in the upper part of Fig. 30, showing the relative proportion of fracture-source to fracture-through plates in each interval in the range 50 to 110° F indicates that this ratio increases more and more rapidly with increasing transition temperatures. This means that the probability that a plate will become the starting point of a fracture, under the conditions existing in a structure such as a ship, increases markedly with increasing transition temperature of the plate, as determined with Charpy V-notch specimens. Similar calculations indicate that the probability of a fracture ending in a plate increases as the transition temperature decreases, as shown by the dot-dash line in the lower section of Fig. 30.

CHEMICAL COMPOSITIONS OF THE STEELS

Chemical analyses were made on all of the plates, and gas analyses, by the vacuum-fusion method, were completed on representative samples from nearly half of the plates.*

None of the steels had a silicon content greater than 0.12%, which indicates that all of the steels tested were either semi-killed or rimmed steels, and that no killed steels were included.

In order to determine whether the notch sensitivity of the steels and the nature of the fractures that occurred in service were related to the chemical compositions, plots similar to Figs. 31 and 32 were made for each element found in the chemical analyses of the steels, and for the gases hydrogen, oxygen and nitrogen. In each section of these figures, the vertical bars at the left represent the range of (element) content for plates of each thickness group in the source, through and end or no-fracture categories, respectively, and the symbols within the bars represent the average values. The content of the element under consideration is also plotted

* Chemical compositions of the majority of the plates tested to date are given in Appendix 3.

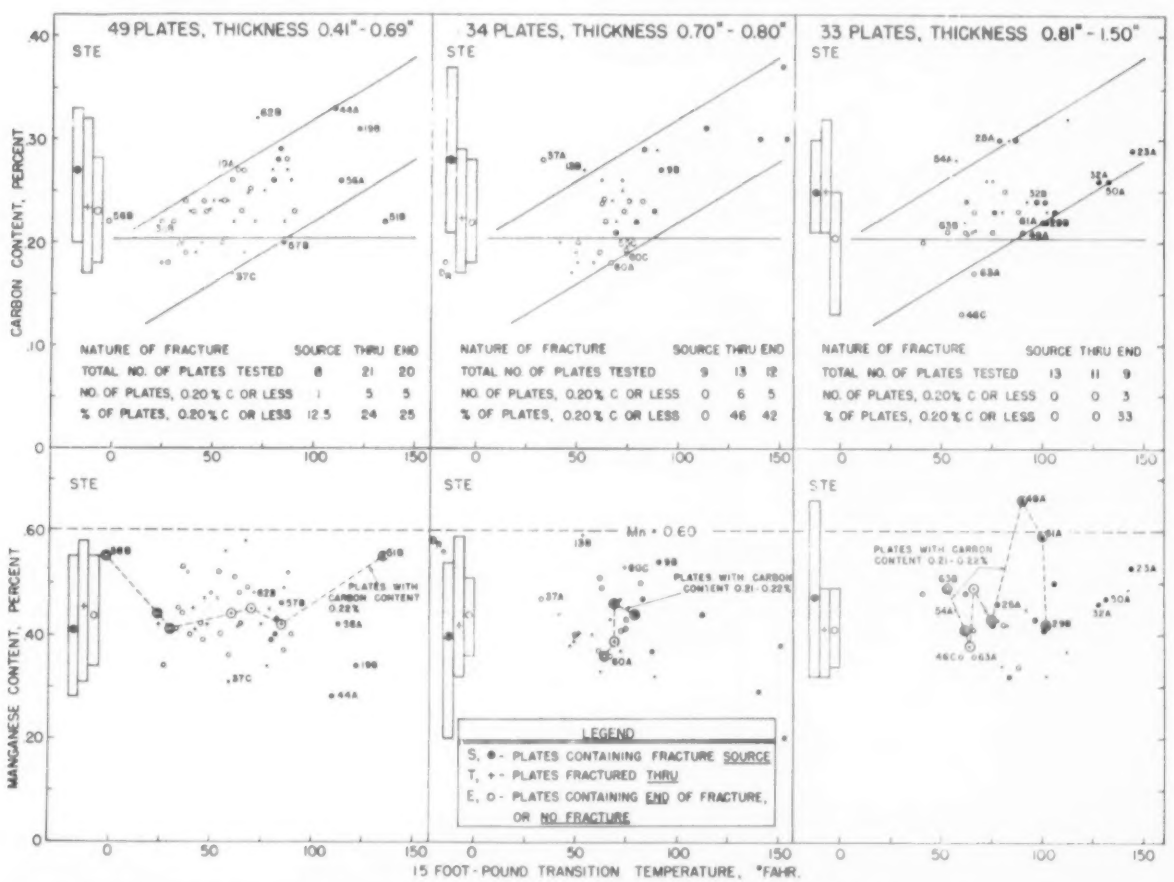
49 PLATES, THICKNESS 0.41" - 0.69"
34 PLATES, THICKNESS 0.70" - 0.80"
33 PLATES, THICKNESS 0.81" - 1.50"
STE
CARBON CONTENT, PERCENT
MANGANESE CONTENT, PERCENT
Mn = 0.60
PLATES WITH CARBON CONTENT 0.22%
PLATES WITH CARBON CONTENT 0.21-0.22%
NATURE OF FRACTURE SOURCE THRU END
TOTAL NO. OF PLATES TESTED 8 21 20
NO. OF PLATES, 0.20% C OR LESS 1 5 5
% OF PLATES, 0.20% C OR LESS 12.5 24 25
NATURE OF FRACTURE SOURCE THRU END
TOTAL NO. OF PLATES TESTED 9 13 12
NO. OF PLATES, 0.20% C OR LESS 0 6 5
% OF PLATES, 0.20% C OR LESS 0 46 42
NATURE OF FRACTURE SOURCE THRU END
TOTAL NO. OF PLATES TESTED 13 11 9
NO. OF PLATES, 0.20% C OR LESS 0 0 3
% OF PLATES, 0.20% C OR LESS 0 0 33
LEGEND
S, ● - PLATES CONTAINING FRACTURE SOURCE
T, + - PLATES FRACTURED THRU
E, ○ - PLATES CONTAINING END OF FRACTURE, OR NO FRACTURE
15 FOOT-POUND TRANSITION TEMPERATURE, °FAHR.

Fig. 31 Relation of carbon and manganese content to nature of fractures and to 15 ft-lb transition temperatures

against the transition temperature, for the individual plates in each thickness group. Different symbols are used for the plates in each of the three categories, as shown in the legend. The NBS plate numbers of some of the plates, especially those in the fringe areas of the scatter diagrams, are also shown.

Carbon: Carbon content (Fig. 31, top) has a pronounced effect on both the nature of fracture and the transition temperature. In each thickness group (except for one plate), the range and the average values of carbon content of plates in the fracture-source category are higher than for plates which did not contain a fracture source. The lowest value of carbon content for a fracture-source plate (all thickness groups combined) was 0.20%. The carbon content was 0.20% or less (below the horizontal line) in 24% of the fracture-through plates, 32% of the fracture-end plates and only 3% of the fracture-source plates. In the scatter diagrams, the majority of the points fall within a broad band (sloping lines) and indicate a general increase of the transition temperature as the carbon content is increased.

Manganese: Manganese content (Fig. 31, bottom) does not show any consistent relationship to either the nature of the fracture or the transition temperatures of the plates in the various thickness groups. Only one of the plates contained more than 0.60% manganese, the minimum required by the present ABS specification[5] for plates more than $^1/_2$ in. thick.

To determine whether any possible effect of manganese was masked by the effect of the carbon content or the manganese-carbon ratio, similar plots were made for plates with various constant levels of carbon content. The dotted lines connecting the circled points in Fig. 31 (representing plates with 0.21–0.22% carbon) are typical of the scatter for the different carbon levels, and show no definite trend. The manganese/carbon ratio was also plotted against the transition temperatures and the relations (not shown) were no more evident than the relation for carbon alone.

Phosphorus (Fig. 32, top): Plates with high phosphorus content (greater than 0.04%) generally had higher transition temperatures, although the trend is not very evident in the figure since high transition temperatures were found also for a number of plates with low phosphorus. The relation between the phosphorus content and the nature of the fractures is more apparent than the relation of phosphorus content to transition temperature, as in all thickness groups the plates with high phosphorus content were in the fracture-source category. Five of the plates contained more than 0.04% phosphorus, the maximum permitted by the present ABS specification.[5] All of these plates contained fracture sources, which were located near welds or flame cuts.

An explanation of the above observations is offered by the work of McGeady and Stout,[22] who reported a correlation between the notch sensitivity of *welded* plates and the carbon content plus 20 times the phosphorus content, whereas for the same plates in the unwelded condition the notch sensitivity apparently was not so closely related to the phosphorus content. Similarly, the fracture sources in the ships were generally associated with welds or flame cuts, while the transition temperatures were determined in unaffected areas of the plates.

Sulfur (Fig. 32, center): Most of the plates had a normal sulfur content of 0.02

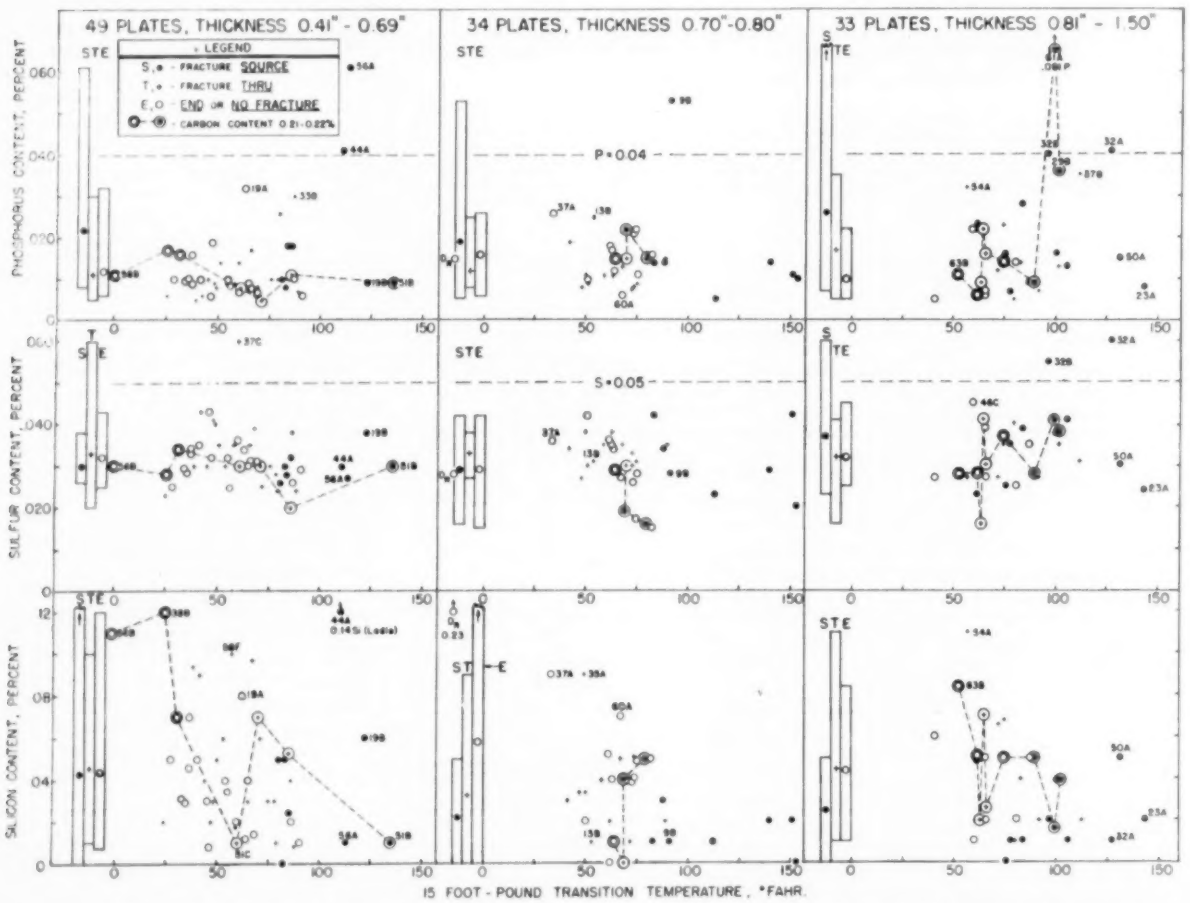

Fig. 32 Relation of phosphorus, sulfur and silicon content to nature of fractures and to 15 ft-lb transition temperatures

to 0.04%, and in this range sulfur apparently has little or no effect.

Silicon (Fig. 32, bottom): Silicon content, in the range 0–0.12%, affects the transition temperature and the nature of fracture in the opposite direction to the effect of carbon. The transition temperatures were generally lower for the plates with higher silicon and, with the exception of one plate, the range and average values of silicon content were lower for the fracture-source plates than for the plates that did not contain a fracture source. The silicon content was greater than 0.06% (the middle of the range) in 20% of the "fracture through" plates, 27% of the "fracture end" plates and only 3.4% of the "fracture source" plates.

Other Elements: Similar plots (not shown) were made for each of the other elements found in significant amounts in the steels (Cu, Ni, Cr, V, Mo, Al and As), and for the gases H_2, N_2 and O_2. No definite relations could be observed between the content of any of these other elements, considered individually, and either the transition temperature or the nature of the fractures. This indicates that the effects of other elements, in the amounts present in steels of this type, are considerably less than the effects of carbon, phosphorus and silicon.

However, many of the other elements were present only in small amounts, or, as in the case of manganese, in limited ranges, in the group of steels under consideration. Thus, it is possible that the effects of the relatively small variations of these other elements might be obscured by the greater effects of carbon, phosphorus and silicon. Tentative correction factors for these three elements, estimated from the slope of the scatter diagrams, were added to the observed transition temperatures of the plates, assuming, provisionally, that the effects of the individual elements might be additive, and that variations due to differences in nature of occurrence or combinations of elements would be small in the steels under consideration. The content of each element was then plotted against the corrected transition temperature, and, with some of the variations at least partially eliminated, the effects of other elements could be seen. Tentative correction factors for these elements were added, and this process was repeated several times. At each step, the estimated correction factors for each element were checked by the slope of the scatter diagrams, and modified if necessary.

A correction factor for the fracture grain size was determined in a similar manner. The addition of this factor practically eliminated the differences between the thickness groups, indicating that the generally higher transition temperatures of the thicker plates were due primarily to larger grain size.

The results obtained by this method indicated that the transition temperatures of the steels studied in this investigation were increased with increasing content of carbon, phosphorus, molybdenum and arsenic and decreased with increasing amounts of manganese, silicon, copper and nickel.

The foregoing results and the method by which they were obtained seem to indicate that the several influences might be incorporated into an "equivalence" formula such as those used for estimation of hardenability or tensile properties. The numerical evaluations of the most important of the factors determined by the above method may be represented tentatively by the following:

15 ft-lb transition temperature (° F) = $70 \pm 30 + 300 \times$ % Carbon + $1000 \times$ % Phosphorus $- 100 \times$ % Manganese $- 300 \times$ % Silicon $- 5 \times$ Fracture Grain Size Number

While not rigidly true, the application of this tentative formula has shown that over 90% of the ship plates that were tested had transition temperatures within the range indicated.

With the information obtained by this empirical method as a guide, more rigorous statistical analyses of the data are in progress, in cooperation with the Statistical Engineering Section of NBS.

It should be noted, however, that this formula may not be applicable for compositions outside of the range 0.15–0.35% carbon, 0.0–0.10% phosphorus, 0.0–0.15% silicon, 0.0–0.25% copper or a total of 0.2% molybdenum, chromium and arsenic combined, as larger amounts of these elements may raise the transition temperature above the limit indicated. Residual nickel, and copper in amounts less than about 0.20% lower the transition temperature by a small amount, and need not be considered in the calculation of the maximum 15 ft-lb transition temperature to be expected. Other elements generally are present in such small amounts in steels of this type that their effects appear to be negligible.

This formula indicates that transition temperatures may be decreased by reducing the carbon or phosphorus content, by increasing the manganese or silicon content or by mill practices that produce a fine grain size. It also shows that silicon, in amounts up to 0.15%, is three times as effective as manganese.

While this work was still in progress, results of an investigation at the Naval Research Laboratory became available. These investigators[23] determined the effects of various elements on the transition temperatures of laboratory heats of steel in which only one element was intentionally varied, and also showed that the effects of carbon, manganese and nickel are very nearly additive.

In the range where the compositions covered by the two investigations were similar, the results obtained by "working backward" from the compositions of the fractured ship plates were in good agreement with the results for the 15 ft-lb transition temperature obtained at NRL by the more straightforward laboratory method. This appears to validate the provisional assumption of an additive relation for the effects of the different elements. However, the two investigations did not agree on the effects of copper and silicon, which illustrates the dangers of optimistic extrapolation. In the low ranges of copper and silicon content found in the ship plates, these elements appear to lower the transition temperature, but in the higher ranges covered by the NRL investigation, the transition temperature is raised by further additions of these elements. Further investigations at NRL, and similar research at another laboratory,[24] using different specimen types, have shown that the notch sensitivity is first lowered and then raised, as the copper or silicon content is increased. The effects of some other elements also change, either in magnitude or direction, as the content of the element is increased.

The work at NRL also showed that some elements, such as carbon, change the shape of the transition curve for Charpy V-notch specimens, so that the effects of these elements are not the same for different criteria of the transition temperature. All of these factors must be considered in evaluating the effects of composition on the notch sensitivity of hot-rolled steels.

There may also be interactions between certain elements that do not show up when only a single element is varied. Additional study is needed on the effects of combinations of elements and of minor variations of residual elements or gases.

The metallurgical effects of welding and flame cutting, and the increase of notch sensitivity resulting from deformation or strain aging, must also be considered, and we need to know more about the relation of these phenomena to the composition of the steels.

Part III. Discussion and Summary

FACTORS INVOLVED IN THE PREVENTION OF FAILURES

There are three principal factors involved in the prevention of failures of welded structures: First, the material, mostly steel plates and weld metal, should possess, uniformly, the properties anticipated by the designer. Furthermore, these properties must not be affected ad-

versely by the effects of fabricating operations or conditions encountered in service. Second, the design should aim to produce, with economy of materials, weight, and cost, a structure functionally suitable for the purpose intended, with adequate strength in all parts of the structure under all possible operating conditions. Third, workmanship or fabrication—the work of the fitters, welders, supervisors and inspectors—should carry out the ideas of the designer and must produce joints of the strength that he intended. Of course, these three factors are somewhat interrelated, and there may be some other factors involved, such as the cost and availability of materials and fabricating facilities.

The failures in the ships usually involved a combination of these three factors, and were largely a result of the lack of adequate knowledge of (*a*) the significance of certain properties of structural steels, especially their relation to the performance of the steel in the presence of multiaxal stresses or in structures containing notches or other discontinuities, (*b*) the effect of fabricating operations such as welding, flame cutting or cold forming, or of service conditions such as low temperatures, on these significant properties of the steels, (*c*) the distribution of stresses, both uniaxial and multiaxial, in welded ships under different operating conditions and (*d*) the presence of welding defects, and the extent or severity of welding defects that may be tolerated in a structure under the conditions encountered in service.

A number of the fractures originated in parts that are not ordinarily considered as strength members (such as scuffing bars, bulwark plates and cap rails, bilge keels or deck attachments) and propagated through welds into the hull structure. This shows that, even for non-strength members that are attached to the hull by welding, the standards of quality of materials, design and workmanship should be equal to those required in the hull structure itself.

The incidence of failures in welded ships has been reduced materially by improvements of design details and of welding workmanship. However, it is evident that stress concentrations caused by unavoidable structural notches or undetected welding defects can never be entirely eliminated. Even if the original construction approaches perfection, a mechanical or metallurgical notch may be formed at any time during the life of a structure as a result of alterations, accidents, repairs, temporary attachments or even accidental touching of the steel by a welding electrode or an electrical conductor. Many of the failures did originate at such notches, introduced after the ship was in service. This constitutes further evidence that the quality of the steel, especially with regard to notch toughness, must be considered as a most important factor in the prevention of failures in welded structures.

It has been shown that the plates with abnormally high notch sensitivity, in which fractures are most likely to originate, represent the relatively few plates whose notch sensitivity falls in the tail of the probability curve for steels of the quality used when these ships were built. This suggests two possible remedies:

1. Improvement of the average quality of the steel with respect to notch sensitivity, by suitable specification requirements such as composition, grain size or heat treatment. (In effect, this would move the entire probability curve in the direction of decreased notch sensitivity, so that a much smaller proportion of the plates in the tail of the curve would fall beyond acceptable limits of notch sensitivity.)

2. Determination of the notch sensitivity of every heat of steel by inspection tests, and rejection of all heats that fail to meet suitable prescribed standards. (In effect, this would "cut off the tail" of the probability curve.)

The first of these remedies would have the additional advantage that a larger proportion of the steels would have notch sensitivities low enough to halt the propagation of any fracture that might start.

In the application of either of the above remedies, however, due consideration should be given to the possibility that the notch sensitivity of the steel as fabricated in the ship may be considerably greater than the notch sensitivity as determined by mill tests of the prime plate. It has been noted previously that, in addition to the factor of design, nearly all of the fractures originating in plates were associated with welds, flame cuts or deformation of the plate, and in a number of cases notched bar tests have indicated that the notch sensitivity is increased locally as a result of these operations. It is conceivable that this increase might be greater in some steels than in others, so that a steel which appeared to be relatively notch tough in the mill tests might be more notch sensitive, as a result of fabricating operations, than some other steel. For example, McGeady and Stout[22] have shown that the increase of notch sensitivity after welding is much greater for some steels than for others of nearly the same notch sensitivity in the as-rolled condition.

SUMMARY

The results of this investigation indicate that the structural failures in welded ships were caused by stress concentrations and by steel that was notch sensitive at the operating temperatures. This conclusion is confirmed by results of laboratory tests of fractured plates, as well as by critical observations of the starting points and the circumstances of the failures.

The starting points of the fractures could be traced, invariably, to a point of stress concentration at a notch resulting from structural or design details, welding defects, metallurgical imperfections or accidental damage. The failures were relatively more numerous at lower operating temperatures, and the few failures that did occur at temperatures higher than about 50° F were not as extensive or serious as many of the failures which occurred at lower temperatures. The fractures in the ships were of a brittle type, showing very little ductility; but in tension tests the steels from the fractured plates showed normal strength and ductility, which would meet the specification requirements under which these steels were purchased.

All of the above features observed in the ship failures are typical of the behavior of a notched specimen tested in tension or in bending, and are indications that notch sensitivity of the steel was a major factor in the failures. However, the fractures occurred in relatively few of each of the different types of ships that were built, and at different locations in these ships, whereas the majority of the ships of the same design and construction did not fracture. These facts suggest that in relation to the conditions of stress concentration that existed in these ships under various operating conditions, the lack of notch toughness was a borderline deficiency of a relatively small portion of the steels used when these ships were built. That is, although similar notches resulting from design details or welding defects were undoubtedly present in all ships of a given type, fractures originated only when high stress concentrations at critical locations in the structure occurred in combination with plates of usually low notch toughness. This is confirmed by the Charpy V-notch tests of the fractured plates, which showed that plates in which fractures originated in the ships were generally more notch sensitive than plates which did not contain the source of a fracture, using as the criterion of notch sensitivity either the 15 ft-lb transition temperature or the energy absorbed by Charpy V-notch specimens at the failure temperatures of the respective plates.

Statistical interpretation of the data shows that under the conditions existing in a structure such as a ship, the probability that a fracture will originate in a plate increases markedly with increasing notch sensitivity of the steel. The plates with abnormally high notch sensitivity, in which fractures are most likely to originate, represent the relatively few plates whose notch sensitivity falls in the tail of the frequency distribution curve for steels of the quality used when these ships were built. Similarly, the probability that a fracture will end in a plate increases with increasing notch toughness. Fractures, once started, will continue to propagate in plates of intermediate notch toughness unless the stress concentration falls below

that required to promote fracture.*

A number of the fractures originated near welds or flame cuts, or at locations where the plate had been deformed in fabrication (such as bilge plates or masts) or during operation. Notched bar tests on some of these plates show that the notch sensitivity in the vicinity of the fracture sources had been further increased by the metallurgical effects of the welding or flame cutting, or by strain aging resulting from deformation. In several cases, fractures originated at arc craters, in plates that were considerably less notch sensitive than the average for fracture-source plates.

The evidence indicates, therefore, that the fractures originated in plates that were "selected" because of higher than average notch sensitivity at critical locations (or notches) in the structure, and that in at least some cases the inherent notch sensitivity of the plates was further increased, in the vicinity of the fracture source, by fabricating operations.

The properties determined by tension tests indicate no relation which is sufficiently marked to differentiate between the plates in which fractures originated and the plates that did not contain fracture sources.

Relations between the Charpy V-notch transition temperatures and the chemical compositions and grain sizes of the ship steels indicate that the notch sensitivity is increased by increasing amounts of carbon or phosphorus, and decreased by increasing amounts of silicon or manganese, or by decreasing the grain size, and that these effects are apparently additive. The improvement of notch toughness that has been attributed to a higher Mn/C ratio is more probably a result of the additive effects of increased manganese and decreased carbon content.

Improvement of the general quality of steels by specification requirements for chemical composition and grain size should be more effective in the prevention of serious failures in ships than a minimum requirement for notch toughness, to be determined by a suitable specification test. The latter would merely eliminate a certain proportion of the potential fracture-source plates, whereas the former would also increase the proportion of potential fracture-end plates.

The results of this investigation indicate the importance of notch sensitivity, and the need for more information on the effects of chemical composition, grain size and mill practices on the inherent notch sensitivity of steels. More knowledge is needed also on the changes of notch sensitivity resulting from various fabricating operations, which may be different for different steels.

ACKNOWLEDGMENTS

Until July 1, 1946, the examinations of fractured ship plates were carried on as a part of an extended research program sponsored by the Bureau of Ships, Navy Department. When that program was terminated, the work was continued by the National Bureau of Standards as a fundamental part of a study of the nature of fracture and of fracture propagation in metals. Since July 1947 the Ship Structure Committee has sponsored the investigations.

Present and former members of the Metallurgy Division of the National Bureau of Standards who have contributed to the work are Melvin R. Meyerson, Gordon L. Kluge, Leo R. Dale, James T. Sterling, H. G. MacKerrow and Lura F. Rochl.

Much credit is due also to the U. S. Coast Guard inspectors, who were responsible for the selection and marking of the samples of fractured plates, and to members of the Ship Structure Committee and of the Merchant Marine Technical Division, U. S. Coast Guard Headquarters, who supplied information regarding the structural features of the ships and the circumstances of the failures, and assisted in many other ways.

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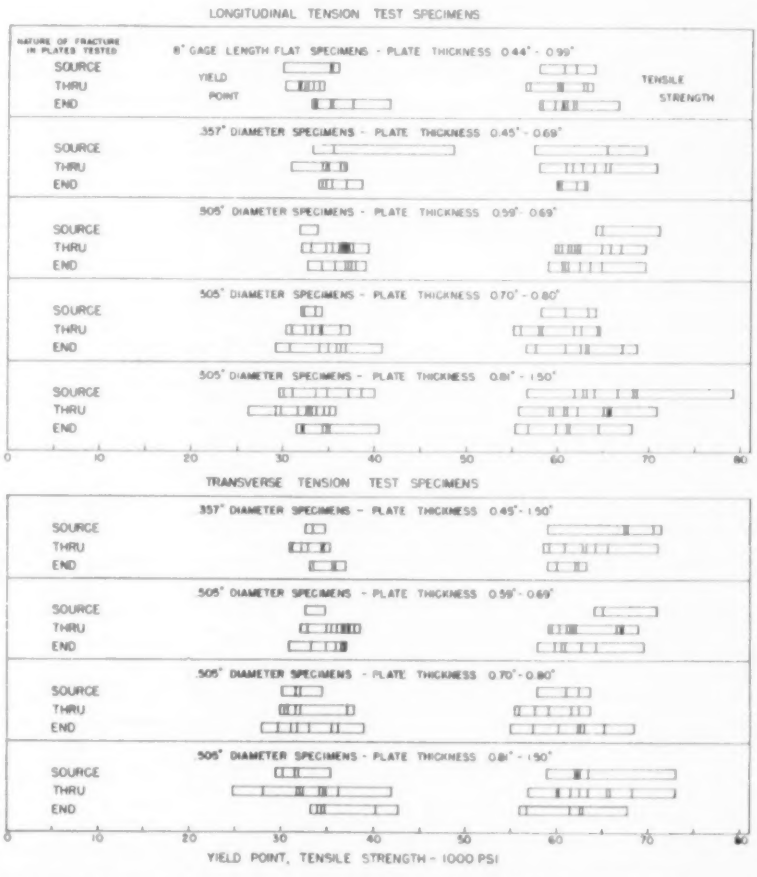
LONGITUDINAL TENSION TEST SPECIMENS
NATURE OF FRACTURE IN PLATES TESTED
8" GAGE LENGTH FLAT SPECIMENS - PLATE THICKNESS 0.44" - 0.99"
SOURCE
THRU
END
YIELD POINT
TENSILE STRENGTH
.357" DIAMETER SPECIMENS - PLATE THICKNESS 0.45" - 0.69"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.59" - 0.69"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.70" - 0.80"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.81" - 1.50"
0
10
20
30
40
50
60
70
80
TRANSVERSE TENSION TEST SPECIMENS
.357" DIAMETER SPECIMENS - PLATE THICKNESS 0.45" - 1.50"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.59" - 0.69"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.70" - 0.80"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.81" - 1.50"
YIELD POINT, TENSILE STRENGTH - 1000 PSI

Fig. 33 Relation of yield point and tensile strength to the nature of fractures in ship plates, for the different plate thickness groups and for longitudinal and transverse tension test specimens of various types

* An even more striking correlation between Charpy V-notch properties and the nature of fracture is shown in the explosion bulge tests conducted at the Naval Research Laboratory,[9] in which many of the unknown or uncontrolled variables involved in the ship failures were eliminated or reproduced under known conditions.

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Appendix 1

SPECIFICATION REQUIREMENTS FOR STRUCTURAL STEEL FOR HULLS

The requirements of the American Bureau of Shipping for tension test properties of structural steel plates not intended for cold flanging are given in Table 5.

Except for the requirement that "structural steels for hulls of vessels shall be made by the 'open-hearth' or 'electric-furnace' process," the ABS specifications before 1948 did not include any requirement for chemical composition or steelmaking practice. Realizing the importance of notch toughness, the ABS revised its specifications in 1948. As a step toward insuring a more notch-tough steel, particularly in the thicker plates, the chemistry and steelmaking practice were specified as shown in Table 6. These requirements were continued without change in the new (1952) ABS specifications.[26]

Table 5—Tension Test Requirements for Hull Plates

	1942–1945[7] specifications	*1948[8] specification*	*1952[26] specification*
Tensile strength, psi	58,000–70,000	59,000–70,000	58,000–71,000
Yield point, min psi	.....	32,000	32,000
Elongation in 8 in., min %*	$\frac{1,500,000}{\text{Tensile strength}}$	$\frac{1,500,000}{\text{Tensile strength}}$	21
Elongation in 2 in., min %	.....	22	22

* Deductions from the required percentage of elongation in 8 in. are allowed for material over $^3/_4$ in. or less than $^5/_{16}$ in. in thickness or diameter.

Table 6—Chemical Composition, Ladle Analysis[8]

	Class A	*Class B*	*Class C*,†*
Plate thickness, in.	$^1/_2$ or less	Over $^1/_2$ to 1, incl.	Over 1
Carbon, max %	.....	0.23	0.25
Manganese, %	.....	0.60–0.90	0.60–0.90
Phosphorus, max %	0.04	0.04	0.04
Sulfur, max %	0.05	0.05	0.05
Silicon, %	.....	.....	0.15–0.30

* Class C plates shall be made with fine grain practice.
† None of the fractured plates included in the foregoing investigation would meet the requirements for Class B or Class C; all were purchased before 1948, under the old specifications.

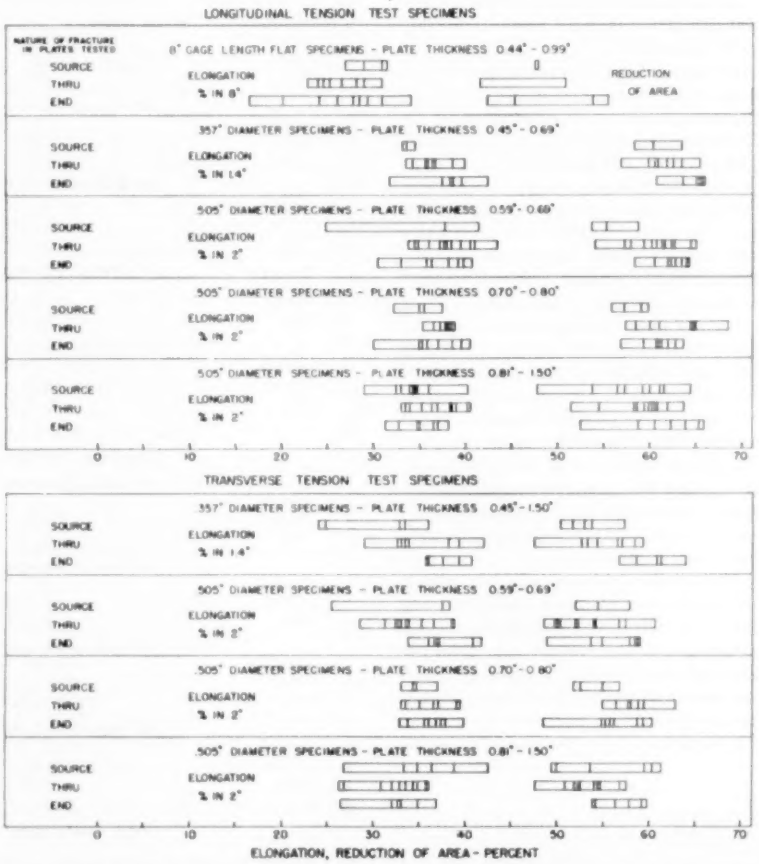
LONGITUDINAL TENSION TEST SPECIMENS
NATURE OF FRACTURE IN PLATES TESTED
8" GAGE LENGTH FLAT SPECIMENS - PLATE THICKNESS 0.44" - 0.99"
SOURCE
THRU
END
ELONGATION % IN 8"
REDUCTION OF AREA
.357" DIAMETER SPECIMENS - PLATE THICKNESS 0.45" - 0.69"
ELONGATION % IN 1.4"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.59" - 0.69"
ELONGATION % IN 2"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.70" - 0.80"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.81" - 1.50"
0
10
20
30
40
50
60
70
TRANSVERSE TENSION TEST SPECIMENS
.357" DIAMETER SPECIMENS - PLATE THICKNESS 0.45" - 1.50"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.59" - 0.69"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.70" - 0.80"
.505" DIAMETER SPECIMENS - PLATE THICKNESS 0.81" - 1.50"
ELONGATION, REDUCTION OF AREA - PERCENT

Fig. 34 Relation of longitudinal and transverse elongation and reduction of area to the nature of fracture in ship plates

Appendix 2

TENSILE TEST PROPERTIES OF FRACTURED SHIP PLATES

The yield points and tensile strengths of plates in the source, through and end categories are compared in Fig. 33, for different plate thickness groups and for longitudinal and transverse tension test specimens of various types. Similar comparisons for the percentage elongation and reduction of area are shown in Fig. 34. There is no distinct difference of the tensile properties of source, through or end plates. The few plates that, by a narrow margin, did not meet the specification requirements for tensile properties show no relation to the types of service failures.

In analyzing these data, due consideration should be given to the following facts: (*a*) Because of size effects and the fact that plates are not homogeneous through the plate thickness, tests made with specimens smaller than the standard 8-in. gage length, full-plate thickness specimens usually show a slightly higher yield point and tensile strength, and a much larger percentage of elongation. However, the elongation and the reduction of area for the smaller specimens were sufficiently high to indicate satisfactory ductility in the static tension test. (*b*) Hot-rolled plates are not homogeneous through the width of the plate or in different plates of the same heat; hence, on a sampling basis, some differences would be expected. (*c*) In some cases the samples had been heated by welding or flame cutting, or had been deformed in fabrication, in subsequent service or incident to the casualty, and thus were not in the original hot-rolled condition.

Appendix 3

CHEMICAL COMPOSITIONS OF THE STEELS

The chemical compositions (check analyses) of the majority of the steels tested are given in Tables 7A, 7B and 7C, for the three thickness groups. The compositions of the additional plates tested since these tables and Figs. 31 and 32 were prepared are in the same range.

Table 7 (*A*)—Chemical Compositions of 49 Plates, 0.41–0.69 In. Thickness

NBS Plate No.	15 ft.-lb. Transition Temperature	Lab.	Chemical Analyses, Percent * = Spectrographic Determinations														Gas Analyses, Percent			Ratio Mn/C
			C	Mn	P	S	Si	Cu	Ni	Cr	V	Mo	Al	Ti	As	Sn	O_2	H_2	N_2	
8 Plates Containing SOURCES of Fractures																				
51B	135° F	N.B.S.	0.22	0.55	0.009	0.030	<.01*	0.03*	0.02*	<.02*	<.01*	<.01*	0.006*	<.01*	0.02*	—	—	—	—	2.50
19B	122	N.B.S.	.31	.34	.009	.038	.06*	.06*	.02*	<.02*	<.003*	.02*	.01*	<.005*	<.03*	<.01*	—	—	—	1.10
56A	113	N.B.S.	.26	.42	.061	.027	.01*	.07*	.04*	.05*	<.01*	<.01*	<.004*	<.01*	.05*	—	—	—	—	1.62
44A	110	U.S.C.G.	.33	.28	.041	.030	.14	—	—	—	—	—	—	—	—	—	—	—	—	0.85
57B	85	N.B.S.	.20	.46*	.018	.032	.024*	.057*	.073*	.057*	<.01*	.007*	.019*	<.01*	.02*	—	—	—	—	2.30
41A	83	N.B.S.	.29	.43	.018	.028	.05*	<.03*	.01*	<.03*	<.003*	.02*	<.006*	<.005*	<.03*	<.01*	—	—	—	1.48
17B	82	N. Y.	.28	.40	.008	.030	.00	.05*	.03*	.03*	.003*	.006*	.022*	.002*	—	—	0.0098	0.0040	0.00011	1.43
27A	80	N.B.S.	.26	.39	.010	.026	.05*	.03*	.01*	<.02*	<.01*	<.01*	.013*	<.01*	<.02*	—	—	—	—	1.50
Average	101		0.269	0.409	0.022	0.030	0.043	0.047	0.029	0.032	0.007	0.012	0.011	0.007	0.03	0.01	0.0098	0.0040	0.00011	1.60
21 Plates Which Were Fractured THRU																				
51A	88	N.B.S.	0.26	0.52	0.007	0.024	<.01*	<.03*	0.02*	<.02*	<.01*	<.01*	0.016*	<.01*	0.03*	—	—	—	—	2.00
33B	86	N.B.S.	.27	.49	.030	.038	.04*	.06*	.04*	<.03*	<.003*	<.003*	.008*	<.005*	<.03*	<.01*	—	—	—	1.81
61E	85	N.B.S.	.22	.42	.011	.020	.053*	.03*	.03*	<.02*	<.01*	<.01*	.008*	<.01*	<.02*	—	—	—	—	1.91
16A	79	N.B.S.	.27	.45	.026	.024	<.01*	.08*	.07*	.06*	<.003*	.01*	.009*	<.005*	<.03*	<.01*	—	—	—	1.67
16B	78	N.B.S.	.28	.34	.010	.028	.03*	.07*	.04*	.03*	<.003*	.01*	<.006*	<.005*	<.03*	<.01*	—	—	—	1.21
15A	75	N. Y.	.25	.42	.010	.030	.03	.02*	.05*	.03*	.003*	.019*	.042*	.002*	—	—	0.0432	0.0039	0.00036	1.68
62B	71	N.B.S.	.32	.48	.005	.025	.060*	.090*	.015*	.020*	<.01*	.003*	.003*	<.01*	<.02*	.004*	—	—	—	1.50
61D	70	N.B.S.	.22	.45	.005	.030	.070*	.05*	.02*	.02*	<.01*	<.01*	.008*	<.01*	<.02*	—	—	—	—	2.04
58C	67	N.B.S.	.25	.58*	.008	.039	.097*	.048*	.018*	.020*	<.01*	<.005*	.002*	<.01*	<.02*	—	—	—	—	2.32
20D	65	N.B.S.	.19	.42	.017	.035	.03*	.17*	.03*	.04*	<.01*	<.01*	.014*	<.01*	.044*	—	.0118	.0066	.00012	2.21
51C	60	N.B.S.	.22	.44	.008	.030	<.01*	.03*	.02*	<.02*	<.01*	<.01*	.047*	<.01*	.02*	—	—	—	—	2.00
37C	59	N.B.S.	.17	.31	.014	.060	<.02*	.12*	.04*	.05*	<.01*	.01*	<.002*	<.01*	.053*	—	.0070	.0070	.00008	1.82
58F	57	N.B.S.	.24	.56*	.009	.035	.10*	.044*	.018*	.021*	<.01*	<.005*	.002*	<.01*	<.02*	—	—	—	—	2.34
5A	53	N.B.S.	.24	.47	.009	.030	.06*	.04*	.16*	.07*	<.01*	.01*	.009*	<.01*	<.02*	—	—	—	—	1.96
30A	50	Phila.	.24	.48	.014	.035	.05	.02	.02	.03	<.001	.006	—	—	.008	—	.0080	.0051	.00010	2.00
37E	48	N.B.S.	.23	.42	.008	.040	.03*	.05*	.03*	.03*	<.01*	.01*	.009*	<.01*	.003*	—	.0056	.0057	.00008	1.83
37F	47	N.B.S.	.23	.47	.009	.041	.02*	.06*	.03*	.03*	<.01*	.01*	.007*	<.01*	.008*	—	.0020	.0063	.00015	2.04
38A	44	N.B.S.	.20	.41	.010	.030	.04*	.05*	.04*	.03*	<.003*	.01*	.006*	<.005*	<.03*	<.01*	—	—	—	2.05
1A	41	N.B.S.	.19	.45	.006	.043	.09*	.13*	.06*	.03*	<.003*	.02*	.004*	<.005*	<.03*	.03*	—	—	—	2.37
61F	38	N.B.S.	.23	.52	.005	.030	.094*	.056*	.024*	.019*	<.004*	.005*	<.005*	<.009*	<.02*	.006*	.0091	.0040	.00023	2.26
57A	24	N.B.S.	.18	.42	.006	.023	<.02*	.15*	.03*	.04*	<.01*	<.01*	<.002*	<.01*	.03*	—	—	—	—	2.33
Average	61		0.233	0.453	0.011	0.033	0.046	0.067	0.038	0.031	0.007	0.009	0.010	0.008	0.024	0.011	0.0124	0.0055	0.00016	1.97
20 Plates Containing END of Fracture or NO FRACTURE																				
38C	90	N.B.S.	0.23	0.40	0.006	0.029	<.01*	<.03*	0.06*	<.03*	<.003*	0.01*	0.020*	<.005*	<.03*	<.01*	—	—	—	1.74
20B	86	N.B.S.	.28	.37	.010	0.026	<.02*	.05*	.03*	.04*	<.01*	.04*	<.002*	<.01*	.011*	—	0.0040	0.0024	0.00026	1.32
62C	68	N.B.S.	.25	.49	.007	.031	.014*	.042*	.015*	.029*	<.01*	.003*	.015*	<.01*	<.02*	<.002*	—	—	—	1.96
6A	65	Phila.	.27	.42	.008	.031	.04	.03	.01	.03	<.001	.002	—	—	.009	—	—	—	—	1.55
62A	64	N.B.S.	.23	.48	.009	.030	.012*	.04*	.02*	.02*	<.01*	<.01*	.010	<.01*	<.02*	—	—	—	—	2.08
19A	62	N.B.S.	.27	.51	.032	.034	.08*	.06*	.09*	.04*	<.003*	.02*	.002*	<.005*	<.03*	<.01*	—	—	—	1.89
22A	59	Phila.	.26	.36	.007	.036	.02	.05	.03	.02	<.001	.006	—	—	.006	—	.0039	.0068	nil	1.38
17A X	55	N.B.S.	.24	.40	.009	.025	.035*	.09*	.03*	.02*	≤.01*	≤.01*	<.01*	≤.02*	≤.02*	≤.01*	.0094	.0052	.00011	1.67
58A	54	N.B.S.	.20	.52	.010	.032	.04*	.05*	.02*	.03*	<.01*	<.01*	.013*	<.01*	<.03*	—	—	—	—	2.60
E.50	46	Ladle	.23	.39	.019	.032	.008	.19	—	—	—	—	—	—	—	—	—	—	—	1.70
17C	45	Phila.	.24	.42	.006	.043	.03	.02	.02	.02	<.001	.000	—	—	.005	—	.0094	.0047	.00010	1.75
25B	40	N. Y.	.23	.40	.010	.035	.05	.01*	.04*	.02*	.003*	.004*	.041*	.002*	—	—	.0382	.0046	.00026	1.74
58D	36	N.B.S.	.19	.44*	.016	.033	.046*	.034*	.012*	.011*	<.01*	<.005*	.014*	<.01*	<.02*	—	—	—	—	2.32
3A	36	N.B.S.	.24	.53	.009	.034	.07*	<.03*	.02*	<.03*	<.003*	.01*	.007*	<.005*	<.03*	<.01*	—	—	—	2.21
55A	34	N.B.S.	.20	.45	.010	.029	.03*	.14*	.05*	<.02*	<.01*	<.01*	.005*	<.01*	.01*	—	—	—	—	2.25
55B	33	N.B.S.	.20	.41	.010	.029	.03*	.05*	.02*	<.02*	<.01*	<.01*	.070*	<.01*	.02*	—	—	—	—	2.05
26A	30	N.B.S.	.22	.41	.016	.034	.07*	.03*	.04*	.04*	<.003*	.01*	<.002*	<.005*	<.03*	<.01*	—	—	—	1.86
10A	27	Phila.	.18	.34	.010	.025	.05	.11	.06	.05	<.001	.02	—	—	.008	—	.0163	.0053	.00015	1.89
38B	24	N.B.S.	.22	.44	.017	.028	.12*	<.03*	.05*	.05*	<.003*	.02*	.007*	<.005*	<.03*	<.01*	—	—	—	2.00
56B	− 2	N.B.S.	.22	.55	.011	.030	.11*	.07*	.04*	.03*	<.01*	<.01*	.007*	<.01*	.02*	—	.0017	.0083	.00010	2.50
Average	48		0.230	0.436	0.012	0.032	0.044	0.056	0.035	0.031	0.006	0.011	0.015	0.007	0.021	0.009	0.0118	0.0052	0.00014	1.89
Average All Plates			0.240	0.438	0.013	0.032	0.044	0.059	0.035	0.031	0.007	0.010	0.012	0.008	0.023	0.010	0.0120	0.0053	0.00015	1.88
Range All Plates			.17-.31	.28-.58	.005-.061	.020-.060	.00-.14	.01-.19	.01-.16	.019-.07	.001-.01	.00-.04	.002-.070	.002-.02	.003-.053	.002-.03	.0017-.0432	.0024-.0083	Nil .00036	0.85-2.60

Table 7 (*B*)—Chemical Compositions of 34 Plates, 0.70–0.80 In. Thickness

NBS Plate No.	15 ft.-lb. Transition Temperature	Lab.	Chemical Analyses, Percent * = Spectrographic Determinations														Gas Analyses, Percent			Ratio %Mn/%C
			C	Mn	P	S	Si	Cu	Ni	Cr	V	Mo	Al	Ti	As	Sn	O_2	N_2	H_2	
9 Plates Containing SOURCES of Fractures.																				
20A	153°F	N. Y.	0.30	0.20	0.010	0.020	0.00	0.01*	0.02*	0.03*	0.003*	0.004*	0.053*	0.002*	—	—	0.0103	0.0038	0.00012	0.67
18A	151	N. Y.	.37	.38	.011	.042	.02	.04*	.06*	.03*	.003*	.030*	.009*	.003*	—	—	.0058	.0032	.00015	1.03
34A	140	Phila.	.30	.29	.014	.029	.02	.03	.02	.02	<.001	.004	—	—	0.007	—	.0208	.0024	.00005	0.97
72A	112	N.B.S.	.31	.44	.005	.023	<.01*	.13*	.03*	.02*	<.01*	.01*	<.01*	<.02*	<.02*	<.01*	-	-	-	1.42
9B	91	Phila.	.27	.54	.053	.028	.01	.28	.12	.11	<.001	.012	—	—	.010	—	.0086	.0044	.00006	2.00
40A	88	Phila.	.23	.37	.014	.034	.03	.02	.04	.02	<.001	.004	—	—	.008	—	—	—	—	1.61
9A	83	Phila.	.29	.47	.014	.042	.01	.24	.10	.05	<.001	.008	—	—	.013	—	—	—	—	1.62
8A	79	N.B.S.	.22	.44	.015	.016	.05*	.15*	.01*	.01*	<.005*	<.003*	.009*	<.005*	<.03*	—	.0108	.0078	.00006	2.00
21A	69	Phila.	.21	.46	.022	.019	.04	.01	.02	.01	<.001	.004	—	—	.008	—	—	—	—	2.19
Average	107		0.278	0.399	0.018	0.028	0.021	0.101	0.047	0.033	0.003	0.009	0.020	0.007	0.014		0.0113	0.0043	0.00009	1.50
13 Plates Which Were Fractured THRU																				
31B	89	N. Y.	0.29	0.32	0.015	0.035	0.02	0.03*	0.05*	0.03*	0.003*	0.006*	0.052*	0.003*	—	—	0.0090	0.0030	0.00012	1.10
60C	74	N.B.S.	.19	.53	.009	.031	.05*	.05*	.02*	.04*	<.01*	<.01*	.048*	<.01*	<.03*	—	—	—	—	2.79
39A	72	N. Y.	.25	.37	.008	.030	.01	.05*	.07*	.03*	.003*	.030*	.015*	.003*	—	—	.0073	.0048	.00014	1.48
20C	72	N.B.S.	.26	.47	.008	.033	.04*	.04*	.02*	.03*	<.01*	<.01*	.014*	<.01*	.013*	—	.0056	.0060	.00007	1.81
37D	69	N. Y.	.22	.39	.015	.030	.00	.05	.03	.04	.003	.023	.006	.002	—	—	.0058	.0033	.00016	1.77
24B	67	Phila.	.24	.43	.013	.035	.05	.02	.05	.03	<.001	.002	—	—	.006	—	.0096	.0068	.00010	1.79
59A	62	N.B.S.	.26	.33	.011	.038	<.02*	.06*	.04*	.03*	<.01*	<.01*	.01*	<.01*	<.02*	—	—	—	—	1.27
60B	58	N.B.S.	.18	.40	.011	.035	.04*	.07*	.03*	<.02*	<.01*	<.01*	.007*	<.01*	<.03*	—	—	—	—	2.22
13B	53	Phila.	.27	.59	.025	.031	.01	.21	.07	.10	<.001	.006	—	—	.010	—	—	—	—	2.18
58E	50	N.B.S.	.18	.40*	.009	.030	.034*	.032*	.012*	.025*	<.01*	<.005*	.005*	<.01*	<.02*	—	—	—	—	2.22
35A	49	N. Y.	.19	.39	.011	.038	.09	.04*	.11*	.03*	.003*	.025*	.013*	.004*	.009*	—	.0220	.0044	.00017	2.05
58B	47	N.B.S.	.17	.38	.008	.027	.034*	.038*	.012*	.022*	<.01*	<.005*	.029*	<.01*	<.02*	—	—	—	—	2.24
30B	41	Phila.	.20	.44	.019	.034	.03	.01	.02	.05	<.001	.006	--	—	.008	—	.0198	.0039	.00008	2.20
Average	62		0.223	0.418	0.012	0.033	0.033	0.054	0.041	0.037	0.006	0.011	0.020	0.007	0.017	--	0.0113	0.0046	0.00012	1.93
12 Plates Containing END of Fracture or NO FRACTURE																				
36A	82	N.B.S.	0.24	0.50	0.016	0.015	0.05*	0.03*	0.01*	<.02*	<.003*	0.01*	0.01*	<.005*	0.08*	<.01*	—	—	—	2.08
24A	75	N.B.S.	.24	.43	.011	.028	.05*	.05*	.02*	.05*	<.01*	<.01*	.015*	--	—	--	0.0079	0.0041	0.00013	1.79
55C	74	N.B.S.	.19	.41	.022	.017	.04*	.02*	.03*	<.02*	<.01*	<.01*	.020*	<.01*	.07*	—	—	—	—	2.15
36B	73	N.B.S.	.23	.41	.021	.026	.04*	.08*	.01*	<.02*	<.003*	.01*	.009*	<.005*	.06*	<.01*	—	—	—	1.78
60A	67	N.B.S.	.18	.36	.006	.027	.07*	.05*	.02*	<.02*	<.01*	<.01*	.009*	<.01*	<.03*	—	—	—	—	2.00
E.75	64	N.B.S.	.22	.36	.015	.029	.01	.17	—	—	—	—	—	—	—	—	—	—	—	1.64
4A	63	N.B.S.	.24	.49	.012	.034	.04*	.04*	.02*	.04*	<.003*	.01*	.007*	<.005*	<.03*	.06*	—	—	—	2.04
39B	62	N. Y.	.24	.51	.017	.035	.00	.01*	.01*	.02*	.003*	.002*	.013*	.002*	—	—	.0084	.0034	.00028	2.13
63C	61	N.B.S.	.19	.37	.018	.036	.052*	.047*	.024*	.054*	<.01*	.003*	.018*	<.01*	<.02*	.006*	—	—	—	1.94
31A	50	N. Y.	.20	.40	.010	.042	.02	.01*	.02*	.02*	.003*	.002*	.022*	.003*	—	—	.0033	.0052	.00010	2.00
37A	33	N.B.S.	.28	.47	.026	.036	.09*	.12*	.03*	.05*	<.01*	.01*	.008*	<.01*	.008*	--	.0072	.0036	.00018	1.68
D_R	- 15	Ladle	.18	.55	.015	.028	.23	.20	—	—	—	—	—	—	—	—	—	—	—	3.05
Average	57		0.219	0.438	0.016	0.029	0.058	0.069	0.019	0.031	0.006	0.008	0.013	0.007	0.043	0.022	0.0067	0.0041	0.00017	2.02
Average All Plates			0.234	0.420	0.015	0.031	0.039	0.070	0.036	0.034	0.005	0.009	0.017	0.006	0.023	0.022	0.0101	0.0044	0.00012	1.86
Range All Plates			.17-.37	.20-.59	.006-.053	.015-.042	.00-.23	.01-.28	.01-.12	.01-.11	.001-.01	.002-.030	.005-.053	.002-.01	.006-.08	.006-.06	.0033-.0220	.0024-.0078	.00005-.00028	0.67-3.05

Table 7 (C)—Chemical Compositions of 33 Plates, 0.81–1.50 In. Thickness

NBS Plate No.	15 lb.-1b. Transition Temperature	Lab.	Chemical Analyses, Percent * = Spectrographic Determinations														Gas Analyses, Percent			Ratio %Mn/%C
			C	Mn	P	S	Si	Cu	Ni	Cr	V	Mo	Al	Ti	As	Sn	O_2	N_2	H_2	
13 Plates Containing SOURCES of Fractures.																				
23A	144°F	N.B.S.	0.29	0.53	0.008	0.024	<.02*	0.03*	<.01*	<.02*	<.01*	0.01*	0.005*	<.01*	<.02*	—	—	—	—	1.83
50A	132	N.B.S.	.26	.47	.015	.030	.05*	.03*	.02*	<.01*	<.01*	<.01*	.01	<.01	<.02	—	—	—	—	1.81
32A	128	Phila.	.26	.46	.041	.060	.01	.17	.06	.03	<.001	.05	—	—	.130	—	—	—	—	1.77
28A	106	N.B.S.	.23	.50	.013	.041	.01	—	Trace	—	—	—	—	—	—	—	—	—	—	2.17
29B	102	Phila.	.22	.42	.036	.038	.04	.03	.03	.06	<.001	.002	—	—	.009	—	0.0125	0.0055	0.00010	1.91
46A	101	N.B.S.	.24	.41	.016	.038	.04*	.04*	.02*	.05*	<.01*	<.01*	.006*	<.01*	<.02*	0.012	—	—	—	1.71
61A	100	N.B.S.	.22	.59	.081	.041	.016*	.006*	.006*	.002*	.004*	<.003	.031*	<.009*	<.02*	<.003*	.0072	.0072	.00019	2.68
32B	97	N.B.S.	.24	.43	.040	.055	<.02*	.16*	.05*	.03*	<.01*	.06*	<.002*	<.01*	.13*	—	—	—	—	1.79
49A	90	N.B.S.	.21	.66	.009	.028	.05*	.02*	.03*	.03*	<.01*	<.01*	.02*	<.01*	<.02*	—	—	—	—	3.14
70A	84	N.B.S.	.30	.32	.028	.039	<.01*	.16*	.03*	.03*	<.005*	<.005*	<.005*	<.009*	.04*	—	—	—	—	1.07
25A	78	N. Y.	.30	.46	.007	.035	.01	.03*	.02*	.02*	.003*	.008*	.008*	.002*	—	—	.0102	.0054	Nil	1.53
12A	76	N. Y.	.23	.42	.016	.025	.00	.17*	.15*	.06*	.003*	.030*	.005*	.003*	—	—	.0076	.0026	.00016	1.83
52A	62	N.B.S.	.24	.48	.023	.023	.05*	.03*	.01*	<.02*	<.01*	<.01*	.03*	<.01*	.06*	.003*	—	—	—	2.00
Average	100		0.249	0.473	0.026	0.037	0.025	0.073	0.034	0.030	0.006	0.017	0.012	0.008	0.047	0.006	0.0094	0.0052	0.00011	1.94
11 Plates Which Were Fractured THRU																				
37B	112	N.B.S.	0.32	0.37	0.035	0.031	<.02*	0.11*	0.03*	0.04*	<.01*	0.01*	<.002*	<.01*	0.060	--	0.0034	0.0040	0.00006	1.15
2B	102	N. Y.	.23	.32	.013	.035	.04	.03*	.02*	.03*	.003*	.002*	.017*	.002*	—	—	.0132	.0046	.00037	1.39
11A	92	N.B.S.	.24	.45	.007	.027	.02	.03	<.10	.02	<.05	<.01	—	—	—	—	.0022	.0058	.00004	1.87
2A	83	Phila.	.30	.42	.014	.039	.04	.03	.02	.06	<.001	.004	—	—	.008	—	.0122	.0035	.00026	1.40
52B	80	N.B.S.	.23	.34	.005	.040	<.01*	.03*	.05*	.03*	<.01*	<.01*	<.001*	<.01*	.02*	0.005*	.0071	.0038	.00042	1.48
63D	75	N.B.S.	.26	.43	.023	.035	.068*	.028*	.010*	<.020*	<.01*	<.002*	.018*	<.01*	<.02*	<.002*	—	—	—	1.65
61C	72	N.B.S.	.26	.45	.012	.027	.066*	.096*	.050*	.043*	<.004*	.006*	<.005*	<.009*	<.02*	.006*	—	—	—	1.73
52C	66	N.B.S.	.21	.49	.016	.030	.026*	.031*	.035*	.047*	<.004*	.005*	.012*	<.009*	<.02*	—	—	—	—	2.33
46B	65	N.B.S.	.21	.42	.022	.041	.07*	.04*	.03*	.05*	<.01*	<.01*	.02*	<.01*	<.02*	.007*	—	—	—	2.00
12B	64	N.B.S.	.21	.38	.009	.016	<.02*	.16*	.20*	.06*	<.005*	.01*	<.002*	<.005*	<.03*	—	.0101	.0034	.00042	1.81
54A	57	N.B.S.	.28	.45	.032	.027	.11*	.01*	.01*	.02*	<.01*	<.01*	.01*	<.01*	<.02*	—	—	—	—	1.61
Average	79		0.250	0.411	0.017	0.032	0.045	0.054	0.050	0.038	0.011	0.007	0.010	0.008	0.024	0.005	0.0080	0.0042	0.00026	1.67
9 Plates Containing END of Fracture or NO FRACTURE																				
33A	88	N.B.S.	0.23	0.34	0.009	0.035	0.05*	<.03*	0.02*	<.03*	<.003*	0.003*	0.009*	<.005*	<.03*	<.01*	—	—	—	1.48
12D	81	N.B.S.	.25	.42	.014	.025	<.02*	.21*	.13*	.07*	<.005*	.01*	<.002*	<.005*	<.03*	—	0.0055	0.0029	0.00013	1.68
12C	75	N.B.S.	.21	.43	.014	.037	.05*	.22*	.03*	.04*	<.005*	<.003*	.05*	<.005*	<.03*	—	.0161	.0066	.00018	2.05
7A	66	Phila.	.23	.41	.007	.039	.02	.04	.04	.03	<.001	.006	—	—	.011	—	—	—	—	1.78
63A	66	N.B.S.	.17	.36	.006	.027	.050*	.028*	.010*	.031*	<.01*	.002*	.043*	<.01*	<.02*	<.002*	—	—	—	2.12
29A	62	N.B.S.	.21	.41	.006	.028	.05*	.25*	.02*	.02*	<.01*	<.01*	.005*	—	.017*	—	.0048	.0050	.00011	1.95
46C	60	N.B.S.	.13	.36	.022	.045	<.01*	.19*	.05*	.03*	<.01*	.01*	<.001*	<.01*	.05*	.03*	—	—	—	2.77
63B	53	N.B.S.	.21	.49	.011	.028	.084*	.066*	.035*	.040*	<.01*	.004*	.002*	<.01*	<.02*	.008*	.0033	.0040	.00066	2.33
13A	41	N.B.S.	.20	.48	.005	.027	.06*	.05*	.05*	.04*	<.01*	<.01*	.018*	—	.012*	—	—	—	—	2.40
Average	66		0.204	0.411	0.010	0.032	0.044	0.120	0.043	0.037	0.007	0.006	0.016	0.007	0.024	0.012	0.0074	0.0046	0.00027	2.06
Average All Plates			0.237	0.435	0.019	0.034	0.037	0.080	0.042	0.035	0.008	0.011	0.013	0.008	0.032	0.008	0.0082	0.0046	0.00022	1.89
Range All Plates			.13-.32	.32-.66	.005-..081	.016-.060	.00-.11	.006-.25	.006-.20	.002-.07	.001 .05	.002-.06	.001-.05	.002-.01	.008-.13	.002-.03	.0022-.0161	.0026-.0072	Nil .00066	1.07-3.14

S.S. Ocean Vulcan Sea Trials

THE Admiralty Ship Welding Committee, in its study of the special problems arising from the use of welding in ships' structures, has carried out a program of investigations to compare the structural behavior of welded and riveted ships under the forces experienced at sea.

There is reproduced, herewith, the conclusions of Report No. R.8, a book of 175 pages.*

These conclusions relate only to the general observations given in the present report and being based only on one ship, namely, the *Ocean Vulcan*, may not necessarily apply to ships of other forms and dimensions. The over-all conclusions from the trials, including those drawn from the detailed analyses (R.9, R.10 and R.11) and for the still-water experiments (R.6 and R.7) are set out in the final report R.12.

VERTICAL LONGITUDINAL BENDING MOMENT

1. The greatest range of vertical bending moment derived from these observations at sea was 190,000 tons-ft, corresponding to a range of stress of 8 tons per sq in. at the top of the sheer strake amidships. There is no experimental evidence to show the actual division of this range between hogging and sagging, but theoretical considerations suggest that the sagging moment constituted about 55% of the total range.

2. The above range of vertical bending moment was associated with waves 35 ft high and between 600 and 700 ft long. According to oceanographical data, waves of even greater severity may be encountered, but it is estimated that for the *Ocean Vulcan* the maximum bending moment range due to waves is never likely to exceed 260,000 tons-ft. This corresponds to a stress range of 11.0 tons per sq in. at the top of sheer strake amidships.

3. Slamming action was observed to cause stress ranges of up to $\pm 1^1/_2$ tons per sq in. at the top of the sheer strake amidships. In the few cases in which slamming stresses were recorded these increased the peak sagging stress but did not increase the peak hogging stress.

HORIZONTAL LONGITUDINAL BENDING MOMENT

4. Horizontal longitudinal bending moments were observed in almost all wave conditions, and sometimes caused stresses of similar magnitude to those due to the concurrent vertical bending. The maximum range of horizontal bending moment observed was 80,000 tons-ft corresponding to a stress range of $2^1/_2$ tons per sq in. at the sheer strake amidships. The horizontal and vertical bending moments were frequently in phase and this resulted in the stress range on one sheer strake (or bilge) being very different from that of the other.

5. The greatest horizontal bending moments occurred when the inclination of the wave advance relative to the ship's course was between 20 and 50 deg. In head or following seas, which caused the highest vertical bending moments, the horizontal bending moments were relatively small.

TORSION

6. Torsion moments derived from the records were small and therefore had little effect on the stresses on the hull girder.

GENERAL

7. The records show that, within the limits of the experimental accuracy, the water pressures measured on the hull were in agreement with those predicted from the trochoidal theory of waves.

8. Theoretical considerations, supported by the experimental evidence, show that for this ship in a seaway the dynamic terms in the determination of the vertical longitudinal bending moments at amidships tend to cancel each other. In consequence a reliable estimate of these bending moments for any particular wave length and height in head or following seas can be made from the assumption that the ship is in static equilibrium on the wave, provided the Smith correction is applied.

9. For the *Ocean Vulcan* the greatest range of bending moment at amidships occurs when the length of the wave approximates to the length of the ship (416 ft-BP) and according to oceanographical data the maximum height ever likely to occur for such a wave is about 35 ft.

* Price $8.00. British Information Services, 30 Rockefeller Plaza, New York, N. Y.

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